



Aalborg Universitet

**AALBORG UNIVERSITY**  
DENMARK

## **A Multistucture Multimode Three-Phase Dual-Active-Bridge Converter Targeting Wide-Range High-Efficiency Performance**

Zia Khan, Akif; Pang Chan, Yiu ; Yaqoob, Muhammad ; Loo, Ka-Hong; Davari, Pooya; Blaabjerg, Frede

*Published in:*

I E E E Transactions on Power Electronics

*DOI (link to publication from Publisher):*

[10.1109/TPEL.2020.3014900](https://doi.org/10.1109/TPEL.2020.3014900)

*Publication date:*

2021

*Document Version*

Early version, also known as pre-print

[Link to publication from Aalborg University](#)

*Citation for published version (APA):*

Zia Khan, A., Pang Chan, Y., Yaqoob, M., Loo, K-H., Davari, P., & Blaabjerg, F. (2021). A Multistucture Multimode Three-Phase Dual-Active-Bridge Converter Targeting Wide-Range High-Efficiency Performance. *I E E E Transactions on Power Electronics*, 36(3), 3078-3098. [9161275].  
<https://doi.org/10.1109/TPEL.2020.3014900>

### **General rights**

Copyright and moral rights for the publications made accessible in the public portal are retained by the authors and/or other copyright owners and it is a condition of accessing publications that users recognise and abide by the legal requirements associated with these rights.

- Users may download and print one copy of any publication from the public portal for the purpose of private study or research.
- You may not further distribute the material or use it for any profit-making activity or commercial gain
- You may freely distribute the URL identifying the publication in the public portal -

### **Take down policy**

If you believe that this document breaches copyright please contact us at [vbn@aub.aau.dk](mailto:vbn@aub.aau.dk) providing details, and we will remove access to the work immediately and investigate your claim.

# A Multi-Structure, Multi-Mode Three-Phase Dual-Active-Bridge Converter Targeting Wide-Range High-Efficiency Performance

Akif Zia Khan, *Student Member, IEEE*, Yiu Pang Chan, *Student Member, IEEE*,  
Muhammad Yaqoob, *Member, IEEE*, Ka-Hong Loo, *Member, IEEE*, Pooya Davari, *Senior Member, IEEE*, and  
Frede Blaabjerg, *Fellow, IEEE*.

**Abstract**—Three-phase dual-active-bridge (3p-DAB) converter is an attractive topology for bidirectional power conversion in high-power applications. However, conduction loss and switching loss are two main loss mechanisms that severely affect its efficiency performance, and adoption of any single modulation scheme or topology cannot minimize these losses over a wide operating range. For this purpose, a reconfigurable topology of 3p-DAB converter is proposed in this paper that utilizes a reconfigurable and tunable resonant network to offer multiple degrees-of-freedom (DoF) in minimizing conduction and switching losses over a wide range of operating conditions. The converter is designed such that for 40 % to 100 % of the rated output power, it operates as a tunable 3p-DAB resonant immittance converter with its output power controlled by varying switching frequency and tuning the resonant frequency of a resonant immittance network to track the switching frequency. Below 40 % of the rated output power, the converter transforms to a tunable 3p-DAB series resonant converter with its output power controlled by varying the impedance of a series resonant network while keeping the switching frequency and phase-shift constant. The combination of both operation modes jointly leads to wide-range zero circulating current and soft-switching of all the switches, and hence a wide-range high-efficiency performance as validated by the experimental results.

**Index Terms**—DC-DC power conversion; immittance converters (ICs); resonant power conversion; series resonant network; unity power factor; zero-voltage switching (ZVS)

## NOMENCLATURE

3p	Three-phase
DAB	Dual-active-bridge
ZVS	Zero voltage switching
SPS	Single phase-shift
DoF	Degree-of-freedom
RMS	Root-mean-square
DABRIC	Dual-active-bridge resonant immittance converter
DABSRC	Dual-active-bridge series resonant converter
RTRN	Reconfigurable and tunable resonant network
TRIN	Tunable resonant immittance network
TSRN	Tunable series resonant network
SCC	Switch-Controlled Capacitor

DFM	Dynamic frequency matching
$D_p$	Duty cycle of switches in the primary-bridge
$D_s$	Duty cycle of switches in the secondary-bridge
$\theta$	Phase-shift between primary-bridge and secondary-bridge ac voltages
$\psi$	Control angle of SCC
$\phi$	Phase angle of port currents
$f_s$	Switching frequency
$f_{s,max}$	Maximum switching frequency
$f_{s,min}$	Minimum switching frequency
$X_A$	Capacitive impedance comprising $L_1$ and $C_1$
$X_B$	Inductive impedance comprising $L_2$ and $C_2$
$\omega_a$	Angular series-resonance frequency of $L_1$ and $C_1$
$\omega_b$	Angular series-resonance frequency of $L_2$ and $C_2$
$\omega_s$	Angular switching frequency
$\omega_r$	Angular resonance frequency of $X_A$ and $X_B$
$\omega_{r,max}$	Maximum angular resonance frequency of $X_A$ and $X_B$
$\omega_{r,min}$	Minimum angular resonance frequency of $X_A$ and $X_B$
$S_{Nx}$	Switches $S_{N1}$ , $S_{N2}$ , and $S_{N3}$ in branch $X_A$ used for transformation of network
$S_{TxA}$	Switches $S_{T1A}$ , $S_{T2A}$ , and $S_{T3A}$ in branch $X_B$ used for tuning $C_{2t}$
$S_{TxB}$	Switches $S_{T1B}$ , $S_{T2B}$ , and $S_{T3B}$ in branch $X_B$ used for tuning $C_{2t}$
$S_{j1}, S_{j2}$	Complimentary switches in the primary-bridge where $j = A, B$ , and $C$ is used for phase-designation
$Q_{j1}, Q_{j2}$	Complimentary switches in the secondary-bridge where $j = A, B$ , and $C$ is used for phase-designation
$C_{2t}$	Tunable capacitors comprising base capacitors $C_2$ and switches $S_{TxA}$ and $S_{TxB}$
$P_{o,max}$	Maximum output power of 3p-DABRIC
$P_{o,min}$	Minimum output power of 3p-DABRIC
$X_{max}$	Maximum impedance of $X_A$ and $X_B$
$X_{min}$	Minimum impedance of $X_A$ and $X_B$
$n$	Transformer's turn ratio
$V_{in}$	Dc input voltage of the converter
$V_o$	Dc output voltage of the converter
$G$	Voltage ratio $nV_o/V_{in}$

A. Z. Khan, Y. P. Chan and K.H. Loo are with the Department of Electronic and Information Engineering, The Hong Kong Polytechnic University, Hong Kong SAR. M. Yaqoob is with Huawei Research Center, Sweden. P. Davari and F. Blaabjerg are with the Department of Energy Technology, Aalborg University, Denmark.

Correspondence: A.Z. Khan (e-mail: akifzia.khan@connect.polyu.hk).

$\epsilon$	Ratio of $P_{o,\max}$ to $P_{o,\min}$
$\sigma$	Ratio of $f_{s,\max}$ to $f_{s,\min}$
$\kappa$	Ratio of maximum $C_{2t}$ to minimum $C_{2t}$

## I. INTRODUCTION

**T**HREE-phase dual-active-bridge (3p-DAB) converter has been widely researched over the past few decades due to its superiority over other bidirectional power converter topologies in terms of modularity, lower component stress, higher power density, smaller input and output current ripple, smaller size of filter elements, galvanic isolation and inherent soft-switching operation [1]. Due to these advantages, 3p-DAB converter has been successfully applied in many emerging high-power applications such as solid-state transformers (SSTs), vehicle-to-grid (V2G) operation, uninterruptible power supplies (UPS) and dc microgrids.

The conventional 3p-DAB converter comprises of two three-phase half bridges interfaced by a three-phase high-frequency isolation transformer. The simplest modulation scheme for 3p-DAB converter is the single phase-shift (SPS) modulation where two phase-shifted high-frequency ac-voltages are applied across transformer's leakage inductances for power flow control. The power flows from the leading to lagging ac-voltage with the phase-shift being used as a control variable to control the output power. However, SPS modulation suffers from the drawbacks of high circulating current and narrow zero-voltage switching (ZVS) range which is sensitive to input-to-output voltage ratio and load conditions. This makes it extremely challenging to maintain a high-efficiency operation of an SPS-controlled 3p-DAB converter for applications where the input-to-output voltage ratio is prone to wide-range variations.

To overcome the drawbacks of SPS modulation, various modulation schemes have been proposed for 3p-DAB converter that make use of additional degree-of-freedom (DoF) for more efficient power flow control at the expense of increased control complexity. The control variables available for modulating 3p-DAB converter are the duty cycles ( $D_p$ ,  $D_s$ ) of the complimentary switches in the primary and secondary bridge, phase-shift ( $\theta$ ) between the primary-bridge and secondary-bridge ac voltages, and the switching frequency ( $f_s$ ). Simultaneous control of two control variables gives rise to a two degree-of-freedom (2-DoF) modulation scheme for 3p-DAB converter. The most popular 2-DoF modulation scheme reported for 3p-DAB converter is the duty-cycle plus phase-shift modulation where the complimentary switches in both active bridges are modulated simultaneously with the same duty cycle (i.e.  $D_p = D_s = D$ ) in conjunction with the modulation of phase-shift  $\theta$  [2]–[5]. The advantages of this modulation scheme stem from its ability to generate and control the pulse-width of the zero voltage levels in the phase voltages (for the duty cycle range  $0 < D < 1/3$ ) that can help to minimize reactive power flow due to the zero voltage levels and improve light-load efficiency. However, with reduced load (i.e. smaller values of  $D$ ), the inductor current and energy flow through the converter becomes increasingly discontinuous. Moreover, difficulty in the selection of optimal values of

multiple control variables ( $D$ ,  $\theta$ ) and high circulating current under heavy-load conditions are some of the drawbacks of this modulation scheme. Another 2-DoF modulation scheme was reported in [6], [7] that utilized phase-shift ( $\theta$ ) and switching frequency ( $f_s$ ) as control variables to achieve a narrow frequency range for output power modulation and simplify close-loop implementation. However, the optimal efficiency performance of this modulation scheme remains highly sensitive to the input-to-output voltage ratio. To further enhance control flexibility, 3-DoF modulation schemes were proposed in [8], [9] that utilized three control variables  $D_p$ ,  $D_s$ , and  $\theta$ , where  $D_p \neq D_s$ , simultaneously for power flow control. These 3-DoF modulation schemes were reported to achieve better performances in terms of reduced conduction loss and extended ZVS range [10]; however, they produce asymmetrical voltage waveforms that lead to increased reactive current flow. In addition, the extensive computational efforts involved in the optimization of three control variables  $D_p$ ,  $D_s$  and  $\theta$  for different input-to-output voltage ratios render their implementation challenging.

In conjunction with the design of modulation schemes, another approach that has been widely researched for enhancing the efficiency of 3p-DAB converter is based on topological variations. In this context, multi-level 3p-DAB converters have been widely investigated that offer the advantages of symmetrical voltage waveforms containing more voltage levels that helps to reduce reactive current flow as well as to lower device stresses. For example, a  $T$ -type three-level 3p-DAB converter was proposed in [11]–[13] that was capable of producing a three-level ( $+V_{DC}/2$ ,  $0$ ,  $-V_{DC}/2$ ) symmetrical voltage waveform. The ability to control the duration of the zero-voltage state with this topology favors the minimization of reactive power flow and extension of ZVS range as compared to two-level ( $+V_{DC}/2$ ,  $-V_{DC}/2$ ) operation of 3p-DAB converter. However, the issue of circulating current remains unsolved as the phase-current continues to circulate in the converter during the zero-voltage state. For reducing the circulating current, a  $\pi$ -type four-level 3p-DAB converter was proposed in [14]. This topology was capable of producing a four-level ( $+V_{DC}/2$ ,  $+V_{DC}/6$ ,  $-V_{DC}/2$ ,  $-V_{DC}/6$ ) symmetrical voltage waveform leading to a more flexible output power control and wide-range zero circulating current. However, increased complexity and higher component count are some of the disadvantages of this topology. Another modified topology of 3p-DAB converter was proposed in [15]–[17] that comprises of three parallel  $120^\circ$  phase-shifted H-bridges at the primary side. This topology offers the freedom to utilize the internal phase-shift between the two legs of the same H-bridge to generate a three-level ( $+V_{DC}$ ,  $0$ ,  $-V_{DC}$ ) symmetrical ac voltage with adjustable duty ratio. This topology was also proposed to limit reactive power flow and reduce conduction loss; however, it still failed to achieve wide-range ZVS operation. The concept of a modified auxiliary-resonant commutated pole was applied to 3p-DAB converter in [18] to achieve wide-range ZVS operation; however, conduction loss cannot be minimized using this approach.

In addition to the different modulation schemes and multi-level topologies, resonant-variants of 3p-DAB converter have

also been investigated extensively with the objective to enhance its efficiency performance. The insertion of different resonant networks to the high-frequency ac-link of 3p-DAB converter offers another DoF in achieving additional desirable characteristics such as dc-bias blocking capability, lower RMS phase current, extended ZVS range, and unity-power-factor operation [19]–[26]. For example, the series-capacitor in a 3p-DAB series resonant converter (3p-DABSRC) helps to remove the dc-bias in the phase-current [27], [28]. Moreover, the sinusoidal current in 3p-DABSRC gives rise to lower RMS value as compared to the piecewise linear current in non-resonant 3p-DAB converter, thus leading to a lower conduction loss. However, the conduction loss and ZVS operation of 3p-DABSRC are still sensitive to the input-to-output voltage ratio. To achieve ZVS operation independent of input-to-output voltage ratio, a 3p-DAB resonant immittance converter (3p-DABRIC) was proposed in [21] which enables a precise control of the phase current phasor and thus, leads to full-range ZVS operation independently of input-to-output voltage ratio. However, it suffers from severe circulating current under light-load conditions leading to poor light-load efficiency. A second mode of operation was proposed for 3p-DABRIC in [21] to achieve in-phase relationship between the voltages and currents at all ports of the immittance network which leads to the elimination of reactive power and minimum conduction loss. However, 33 % of the switches undergo hard-switching in this mode of operation.

From the extensive literature review conducted, it can be concluded that it is very challenging if not impossible to achieve full-range ZVS operation and wide-range zero circulating current simultaneously with any one modulation scheme or topology of 3p-DAB converter. This has motivated us to propose the idea of a reconfigurable topology and modulation scheme that can dynamically adapt to varying operating conditions while ensuring that conduction loss and switching loss are minimized simultaneously over the whole operating range. To meet this objective, a reconfigurable topology of 3p-DAB converter is proposed in this paper that utilizes a reconfigurable and tunable resonant network for achieving a multiple DoF control of the 3p-DAB converter with the objective of maximizing its efficiency under widely varying operating conditions. The converter is designed such that from 40 % to 100 % of the rated output power, it operates as a tunable 3p-DABRIC with its output power controlled by varying the switching frequency and tuning the resonant frequency of the immittance network by means of a switch-controlled capacitor (SCC). This gives rise to a unity-power-factor operation that eliminates all circulating current and reactive power flow. Moreover, all switches undergo ZVS independently of the input-to-output voltage ratio under this operation mode. This allows boosting the efficiency performance for medium-to-high output power levels. Below 40 % of the rated output power, the converter transforms to a tunable 3p-DABSRC with its output power controlled by varying the impedance of a series resonant network using SCC while keeping the switching frequency and phase-shift at the optimal values as determined by the input-to-output voltage ratio. This operation mode ensures minimum-tank-current operation that minimizes

conduction loss and full-range ZVS operation is achieved for all switches leading to enhanced efficiency performance for low-to-medium output power levels. The combination of both operation modes, therefore, leads to a wide-range high-efficiency performance of the proposed converter by minimizing both the switching and conduction losses simultaneously.

The paper is organized as follows. Section II introduces the proposed three-phase reconfigurable and tunable resonant network. The proposed 3p-DAB topology is discussed in Section III. The modeling and analysis of the two operating modes of the proposed 3p-DAB topology, namely, 3p-DABRIC and 3p-DABSRC, are discussed in Sections IV and V, respectively. The prototype design and experimental results are discussed in Section VI. Finally, the concluding remarks are presented in section VII.

## II. PROPOSED THREE-PHASE RECONFIGURABLE AND TUNABLE RESONANT NETWORK

The proposed three-phase reconfigurable and tunable resonant network (3p-RTRN) is a delta-connected network consisting of two types of impedances labeled as  $\mathbf{X}_A$  and  $\mathbf{X}_B$  as shown in Fig. 1.  $\mathbf{X}_A$  consists of switch  $S_{Nx}$ , inductor  $L_1$  and, capacitor  $C_1$ , whereas  $\mathbf{X}_B$  consists of inductor  $L_2$ , capacitor  $C_2$  and switches  $S_{TxA}$ ,  $S_{TxB}$  (where  $x = 1, 2$ , and 3).  $\mathbf{X}_A$  is constrained to operate below the series resonance frequency  $\omega_a$  of  $L_1$  and  $C_1$ , i.e.  $\omega_a > \omega_s$ , to constitute an overall capacitive impedance. On the contrary,  $\mathbf{X}_B$  is constrained to operate above the series resonance frequency  $\omega_b$  of  $L_2$  and  $C_2$ , i.e.  $\omega_b < \omega_s$ , to constitute an overall inductive impedance. The switches  $S_{Nx}$  in  $\mathbf{X}_A$  are used to connect or isolate the

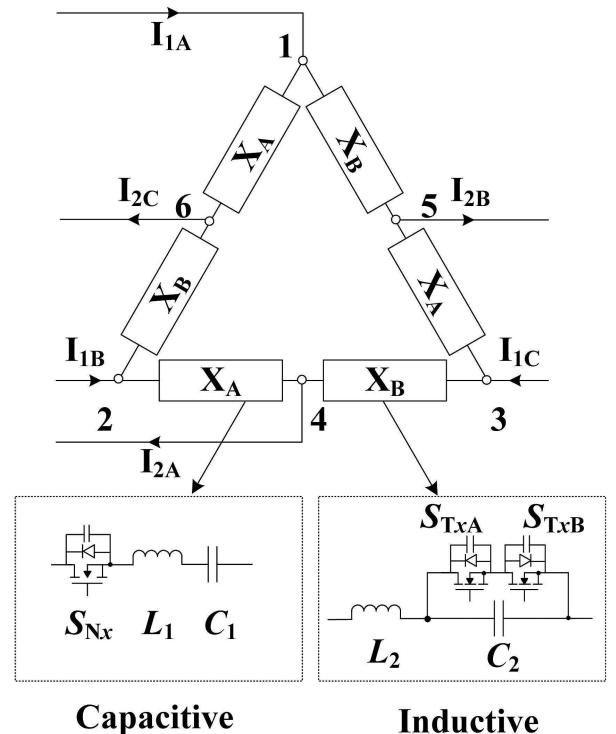


Fig. 1. Proposed reconfigurable and tunable resonant network.



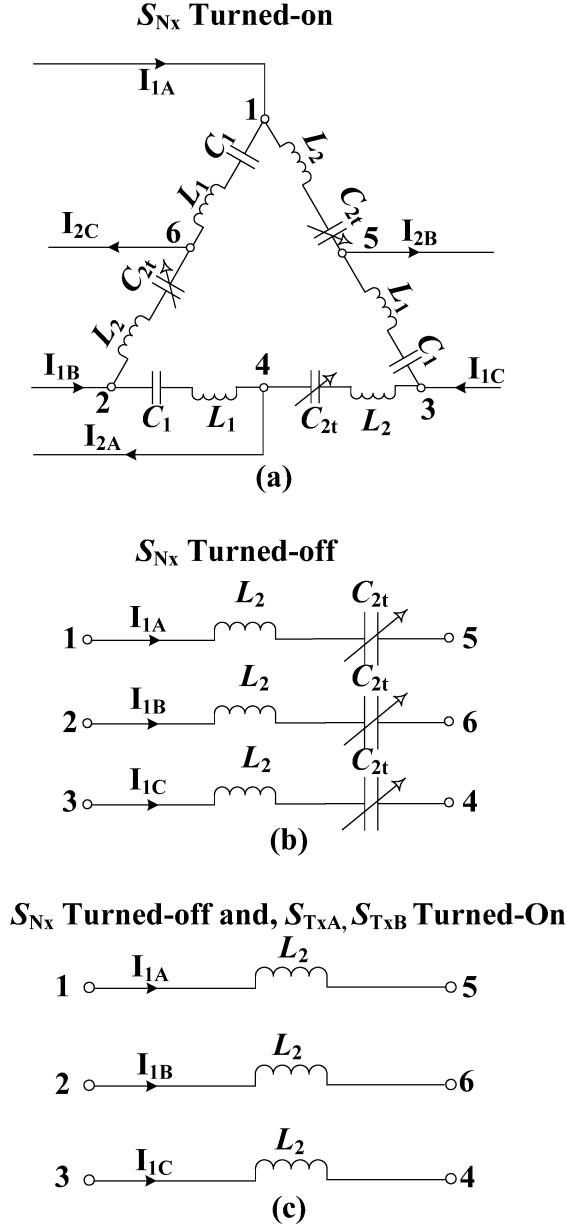


Fig. 2. (a) Three-phase tunable resonant immittance network; (b) Three-phase tunable series resonant network; and (c) Three-phase series inductors.

capacitive impedance  $X_A$  from the network, thus enabling the transformation of the network into different configurations. It should be noted that  $X_A$  can be fully isolated using only a two-quadrant switch as the body diode of  $S_{Nx}$  is kept in a reverse biased state by the voltage across  $C_1$  when  $S_{Nx}$  are turned off.

The switches  $S_{TxA}$  and  $S_{TxB}$  in  $X_B$  are used to electronically control the capacitance of  $C_2$ , thus enabling the control of the series impedance and tuning of the resonance frequency  $\omega_b$  of  $L_2$  and  $C_2$ . Regarding the labeling of the network terminals shown in Fig. 1, the following conventions are adopted: terminals 1, 2, and 3 represent the input ports of the network, whereas terminals 4, 5, and 6 represent the output ports of the network. Furthermore, subscripts  $1j$  (where  $j = A, B, \text{ and } C$ ) are used to denote the parameters (e.g., line currents)

at the input ports of the network, whereas the subscripts  $2j$  (where  $j = A, B, \text{ and } C$ ) are used to denote the parameters (e.g., line currents) at the output ports of the network.

Depending upon the state of switches  $S_{Nx}$ ,  $S_{TxA}$  and  $S_{TxB}$ , 3p-RTRN can transform between three different network configurations. The configuration of Fig. 2(a) is obtained when  $S_{Nx}$  are turned on. This action connects the capacitive impedance  $X_A$  to the network and thus transforms the network to a three-phase tunable resonant immittance network (3p-TRIN). To simplify the drawing,  $S_{Nx}$  are not shown in Fig. 2(a) whereas  $S_{TxA}$ ,  $S_{TxB}$  and  $C_2$  are represented by a tunable capacitor  $C_{2t}$ . The configuration of Fig. 2(b) is obtained when  $S_{Nx}$  are turned off. This action isolates  $X_A$  from the network and thus transforms the network to a three-phase tunable series resonant network (3p-TSRN). The third configuration shown in Fig. 2(c) is obtained by turning  $S_{Nx}$  off and turning  $S_{TxA}/S_{TxB}$  on. This action isolates  $X_A$  from the network and bypasses  $C_2$  in  $X_B$ . In doing so, the network transforms to a conventional three-phase series inductor network.

The reconfigurability of the proposed network highlights the flexibility that it offers to the power control of 3p-DAB converter. Since 3p-TRIN and 3p-TSRN offer the desirable characteristics of dc-bias blocking capability, lower RMS phase current, full-range ZVS, and unity-power-factor operation, the scope of present work is confined to the tunable resonant networks. The subsequent sections will discuss in detail the application of 3p-TRIN and 3p-TSRN in the 3p-DAB converter for improving its efficiency performance.

### III. PROPOSED 3P-DAB CONVERTER WITH 3P-RTRN

The schematic diagram of the proposed converter is shown in Fig. 3. The proposed converter comprises of two three-phase half-bridges interfaced by three Y-Y connected high-frequency isolation transformers and the 3p-RTRN. The complimentary switch pairs  $S_{j1}/S_{j2}$  and  $Q_{j1}/Q_{j2}$  (where  $j = A, B, \text{ and } C$ ) in the primary and secondary bridge are operated with a fixed duty-cycle of 0.5 and variable switching frequency  $f_s$ ,  $f_{s, \min} \leq f_s \leq f_{s, \max}$ . Moreover, the complimentary switch pairs in the adjacent phase legs of primary and secondary bridge are phase-shifted by  $120^\circ$ . (e.g.,  $S_{A1}/S_{A2}$  and  $S_{B1}/S_{B2}$  have a phase-shift of  $120^\circ$ ). In addition, there exists an external phase-shift  $\theta$  between the complimentary switch pairs of the corresponding phase legs in the primary and secondary bridge (e.g.,  $S_{A1}/S_{A2}$  and  $Q_{A1}/Q_{A2}$  are phase-shifted by  $\theta$ ). In this way, the primary bridge generates three two-level  $(+V_{in}/2, -V_{in}/2)$  high-frequency square-wave ac voltages  $v'_{1j}(t)$  (where  $j = A, B, \text{ and } C$ ) having a fixed  $120^\circ$  phase-shift between the adjacent phase voltages. Similarly, a second set of three-phase two-level  $(+V_o/2, -V_o/2)$  square-wave ac voltages  $v_{2j}(t)$  (where  $j = A, B, \text{ and } C$ ) are generated by the secondary bridge. Moreover,  $v'_{1j}(t)$  and  $v_{2j}(t)$  are phase-shifted by  $\theta$  with  $\theta$  defined as positive when  $v'_{1j}(t)$  leads  $v_{2j}(t)$ .

For ease of analysis, the primary side voltages  $v'_{1j}(t)$  and currents  $i'_{1j}(t)$  are reflected to the secondary side (i.e.,  $v_{1j}(t)$  and  $i_{1j}(t)$ ) to obtain an equivalent circuit without transformers. The three-phase square wave ac voltages  $v_{1j}(t)$  and  $v_{2j}(t)$  are

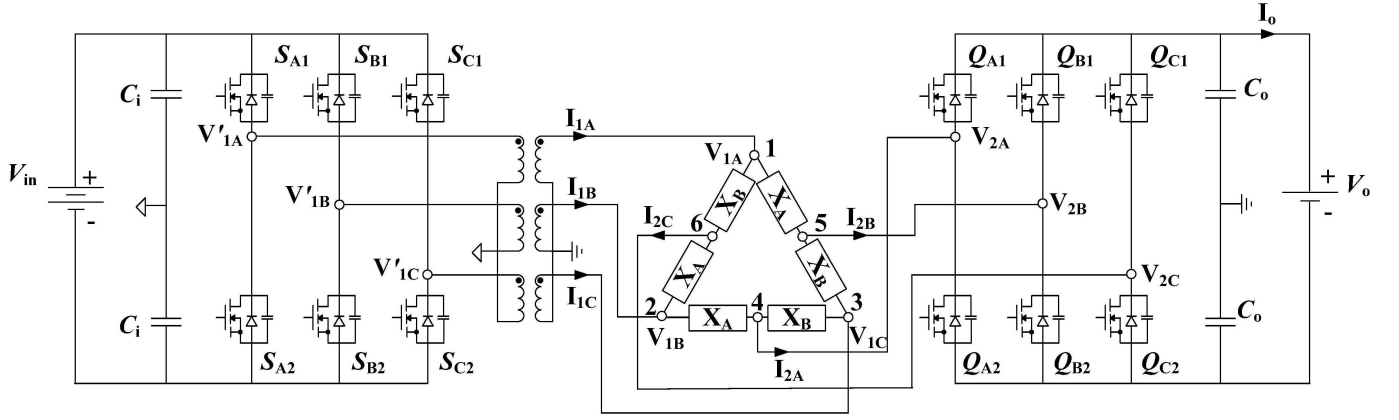


Fig. 3. Proposed 3p-DAB converter with 3p-RTRN.

subsequently represented by their Fourier series expansions as given by (1) and (2).

$$v_{1j}(t) = \frac{2V_{in}}{n\pi} \sum_{k=1,3,\dots}^{\infty} \frac{1}{k} \sin\left(k\omega_s t + f(j)\frac{2\pi}{3}\right) \quad (1)$$

$$v_{2j}(t) = \frac{2V_o}{\pi} \sum_{k=1,3,\dots}^{\infty} \frac{1}{k} \sin\left(k\omega_s t - k\theta + f(j)\frac{2\pi}{3}\right) \quad (2)$$

where  $f(j) = (0, +1, -1)$  when  $j = (A, B, C)$ ,  $\omega_s = 2\pi f_s$  and  $n = \frac{N_p}{N_s}$  is the transformer's turns ratio.

It is assumed that higher-order voltage and current harmonics are attenuated by the resonant network. By applying fundamental component analysis (FCA), the fundamental components of  $v_{1j}(t)$  and  $v_{2j}(t)$  can be represented in phasor form as  $\mathbf{V}_{1j}$  and  $\mathbf{V}_{2j}$  respectively. Hence,  $\mathbf{V}_{1j}$  and  $\mathbf{V}_{2j}$  can be written as (3) and (4).

$$\begin{aligned} \mathbf{V}_{1j} &= V_{1j} \angle \left(0 + f(j)\frac{2\pi}{3}\right) \\ &= \frac{\sqrt{2}V_{in}}{n\pi} \angle \left(0 + f(j)\frac{2\pi}{3}\right) \end{aligned} \quad (3)$$

$$\begin{aligned} \mathbf{V}_{2j} &= V_{2j} \angle \left(\theta + f(j)\frac{2\pi}{3}\right) \\ &= \frac{\sqrt{2}V_o}{\pi} \angle \left(\theta + f(j)\frac{2\pi}{3}\right) \end{aligned} \quad (4)$$

Depending upon the state of switches  $S_{Nx}$  in the 3p-RTRN, the proposed converter can transform between a tunable 3p-DABRIC and a tunable 3p-DABSRC. The detailed discussions on the modeling and analysis for each of the two network configurations are presented in the following sections.

#### IV. MODELING AND ANALYSIS OF TUNABLE 3P-DABRIC

##### A. Power Flow Analysis

Considering the case when the switches  $S_{Nx}$  in branch  $\mathbf{X}_A$  are turned on, the impedances  $\mathbf{X}_A$  enter into the resonant

network and the converter transforms to a tunable 3p-DABRIC that can be represented by the equivalent circuit as shown in Fig. 4. The magnitude of the impedances  $\mathbf{X}_A$  and  $\mathbf{X}_B$  are given by (5) whereas the angular series-resonance frequencies  $\omega_a$  and  $\omega_b$  of  $\mathbf{X}_A$  and  $\mathbf{X}_B$  are given by (6).

$$|\mathbf{X}_A| = X_A = \left| \omega_s L_1 - \frac{1}{\omega_s C_1} \right|$$

$$|\mathbf{X}_B| = X_B = \left| \omega_s L_2 - \frac{1}{\omega_s C_{2t}} \right| \quad (5)$$

$$\begin{aligned} \omega_a &= \frac{1}{\sqrt{L_1 C_1}} \\ \omega_b &= \frac{1}{\sqrt{L_2 C_{2t}}} \end{aligned} \quad (6)$$

Recall that  $\mathbf{X}_A$  is constrained to operate below  $\omega_a$  (i.e.,  $\omega_a > \omega_s$ ) to constitute an overall capacitive impedance to the network. On the contrary,  $\mathbf{X}_B$  is constrained to operate above  $\omega_b$  (i.e.,  $\omega_b < \omega_s$ ) to constitute an overall inductive impedance

##### $S_{Nx}$ Turned-on (3p-DABRIC)

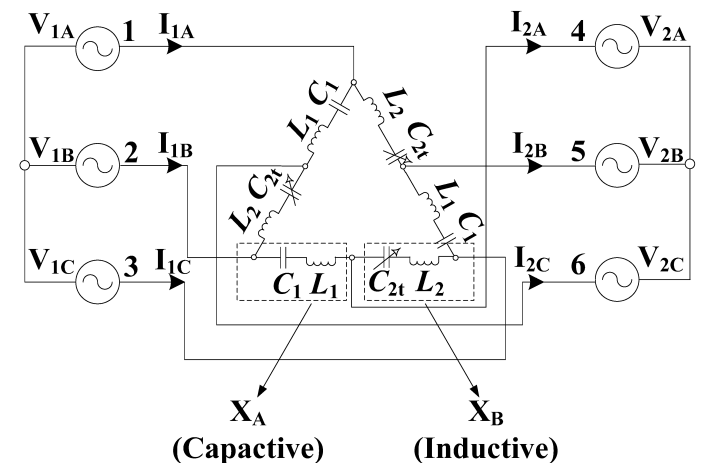


Fig. 4. Equivalent circuit of the proposed converter with 3p-TRIN.

to the network. There exists a specific frequency at which the impedances  $\mathbf{X}_A$  and  $\mathbf{X}_B$  are equal in magnitude and opposite in phase such that they resonate with each other. The resonance frequency at which  $\mathbf{X}_A = -\mathbf{X}_B$  can be found by solving  $|\mathbf{X}_A| = |\mathbf{X}_B|$  and the resulting expression is given by (7).

$$\omega_r = \sqrt{\frac{(C_1 + C_{2t})}{C_1 C_{2t} (L_1 + L_2)}} \quad (7)$$

When operated at  $\omega_r$  (i.e.,  $\omega_r = \omega_s$ ), the resonant network exhibits immittance characteristics, and the line currents  $\mathbf{I}_{1j}$  at the input ports become linearly dependent on the phase voltages  $\mathbf{V}_{2j}$  at the output ports. Similarly, the line currents  $\mathbf{I}_{2j}$  at the output ports become linearly dependent on the phase voltages  $\mathbf{V}_{1j}$  at the input ports. In other words, source transformation occurs at  $\omega_r = \omega_s$  and the voltage sources  $\mathbf{V}_{1j}$  at the input ports are transformed into current sources  $\mathbf{I}_{2j}$  at the output ports. Under immittance conditions  $|\mathbf{X}_A| = |\mathbf{X}_B| = X$ , the expressions for the line currents  $\mathbf{I}_{1j}$ ,  $\mathbf{I}_{2j}$  at input and output ports can be found by applying nodal analysis at different nodes of the resonant network and the results are given by (8) and (9). The detailed derivation of these expressions and a detailed analysis of the effects of component tolerances on the immittance network's characteristics has been presented previously in [21] and will not be repeated here due to space constraint.

$$\begin{aligned} \mathbf{I}_{1j} &= I_{1j} \angle -(\theta + f(j) \frac{2\pi}{3}) \\ &= \frac{\sqrt{6}V_o}{\pi X} \angle -(\theta + f(j) \frac{2\pi}{3}) \end{aligned} \quad (8)$$

$$\begin{aligned} \mathbf{I}_{2j} &= I_{2j} \angle (0 + f(j) \frac{2\pi}{3}) \\ &= \frac{\sqrt{6}V_{in}}{n\pi X} \angle (0 + f(j) \frac{2\pi}{3}) \end{aligned} \quad (9)$$

$$\text{where } X = \left| \omega_r L_1 - \frac{1}{\omega_r C_1} \right| = \left| \omega_r L_2 - \frac{1}{\omega_r C_{2t}} \right|.$$

By inspecting (3), (4), (8) and (9), it can be seen that there exists a phase difference equal to the phase-shift  $\theta$  between  $\mathbf{V}_{1j}$ ,  $\mathbf{I}_{1j}$  and between  $\mathbf{V}_{2j}$ ,  $\mathbf{I}_{2j}$ . By setting  $\theta = 0$ , in-phase relationship (i.e., unity power factor operation) can be achieved between  $\mathbf{V}_{1j}$ ,  $\mathbf{I}_{1j}$  and between  $\mathbf{V}_{2j}$ ,  $\mathbf{I}_{2j}$  at all ports of the immittance network as depicted in the phasor diagrams of Fig. 5.

Considering the converter as being lossless, the expression for the converter's output power  $P_o$  can be derived by finding the output power expression for a single phase (e.g., phase A) and multiplying the resulting expression by three, as given by equation (10).

$$P_o = 3 \times \Re[\mathbf{V}_{1A} \mathbf{I}_{1A}^*] = 3 \times \Re[\mathbf{V}_{2A} \mathbf{I}_{2A}^*]$$

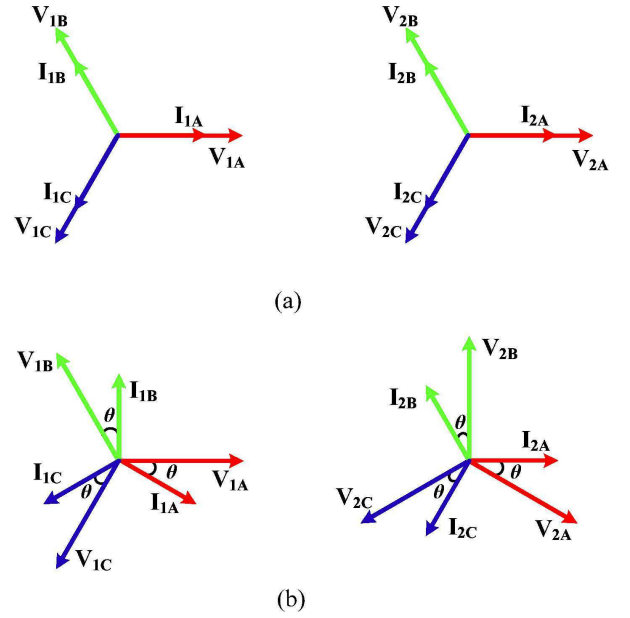


Fig. 5. (a) Phasor relationship between  $\mathbf{V}_{1j}$ ,  $\mathbf{I}_{1j}$  and between  $\mathbf{V}_{2j}$ ,  $\mathbf{I}_{2j}$  for  $\theta = 0$ ; (b) Phasor relationship between  $\mathbf{V}_{1j}$ ,  $\mathbf{I}_{1j}$  and between  $\mathbf{V}_{2j}$ ,  $\mathbf{I}_{2j}$  for  $\theta \neq 0$ .

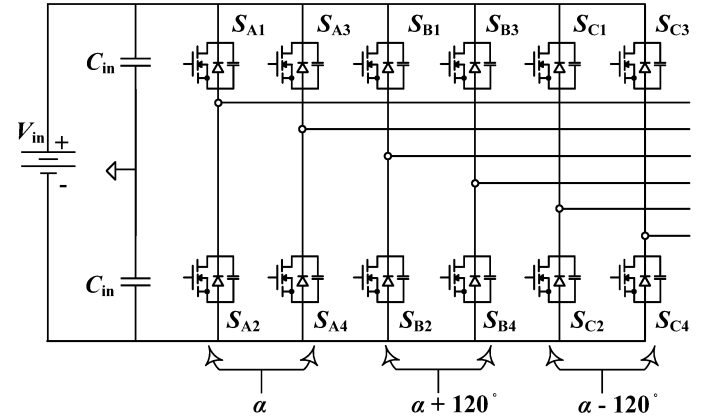


Fig. 6. Three-phase full-bridge structure proposed in [21], [26].

$$= \frac{3\sqrt{12}V_{in}V_o}{n\pi^2 X} \cos(\theta) \quad (10)$$

Inspection of (10) reveals that one of the control variables available for modulating output power of 3p-DABRIC is the phase-shift  $\theta$ . However, since  $\theta$  is constrained to be zero for achieving unity power factor operation, it is necessary to utilize other control variables for controlling the output power. The authors have previously proposed an application of three-phase full-bridge in 3p-DABRIC with which the internal phase-shift  $\alpha$  between the two legs of the same full-bridge is used as a control variable for modulating output power (cf. Fig. 6) [21], [26]. By utilizing an internal phase shift between  $S_{j1}/S_{j2}$  and  $S_{j3}/S_{j4}$  in the range  $0 \leq \alpha \leq 180^\circ$ , three-phase three-level  $(+V_{in}, 0, -V_{in})$  pulse-width modulated symmetrical ac voltages  $\mathbf{V}_{1j}$  with variable duty ratio can be generated by this bridge structure. In this way, the converter's output power can be controlled by modulating the pulse-

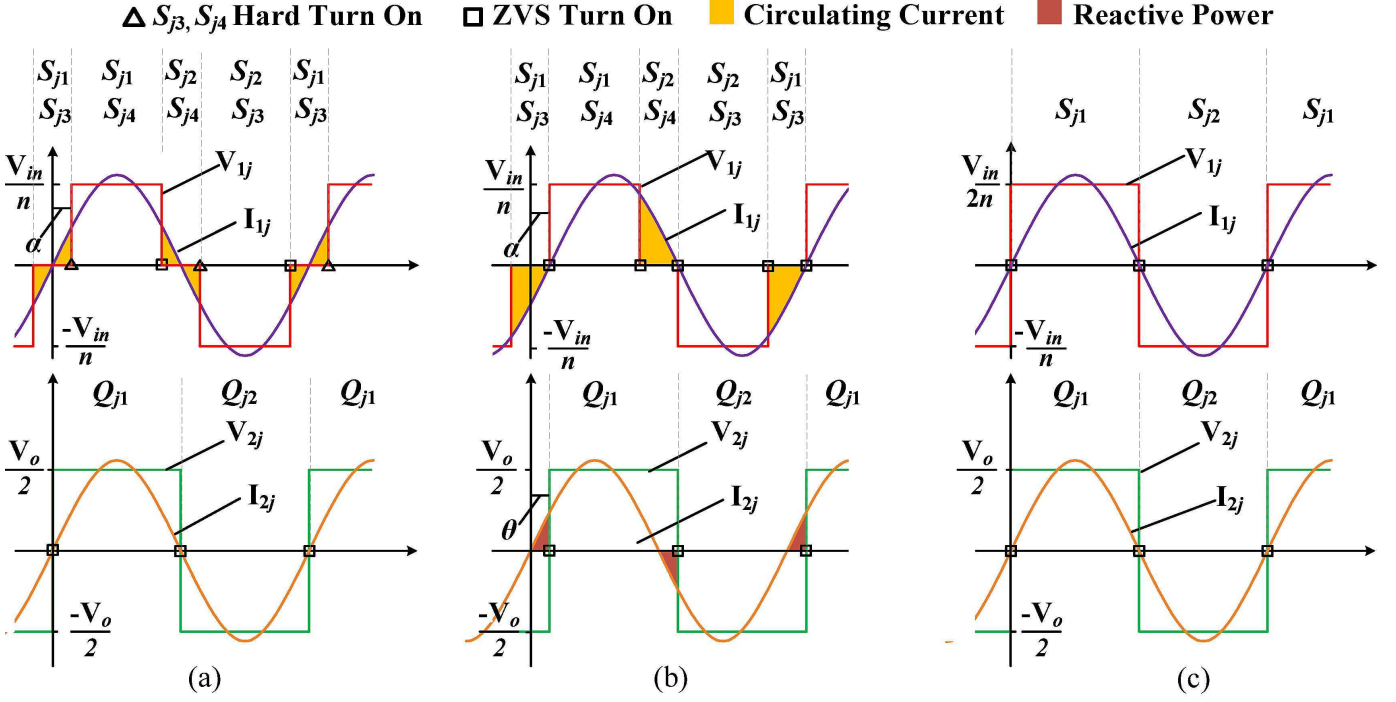


Fig. 7. (a) Port currents and port voltages for unity power factor operation; (b) Port currents and port voltages for full-range ZVS operation; and (c) Port currents and port voltages for proposed DFM modulation.

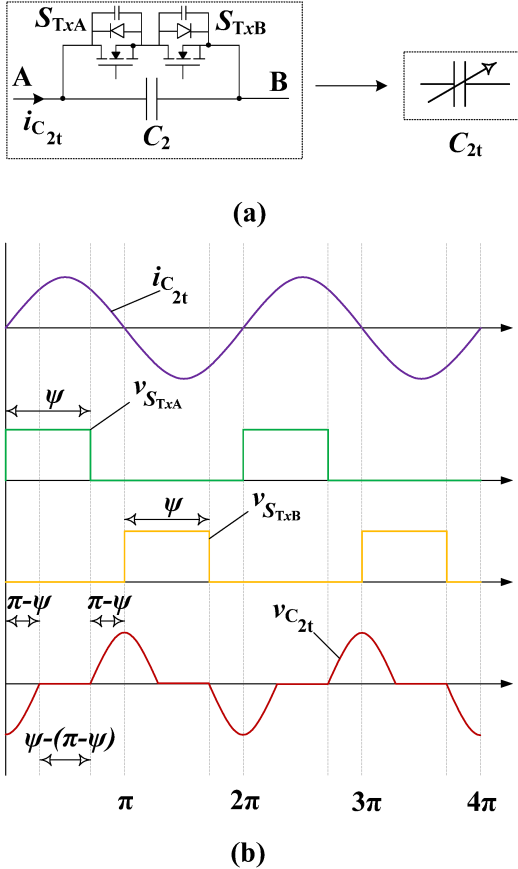


Fig. 8. (a) Schematic diagram of switch-controlled capacitor (SCC); (b) Timing diagram of switch-controlled capacitor (SCC).

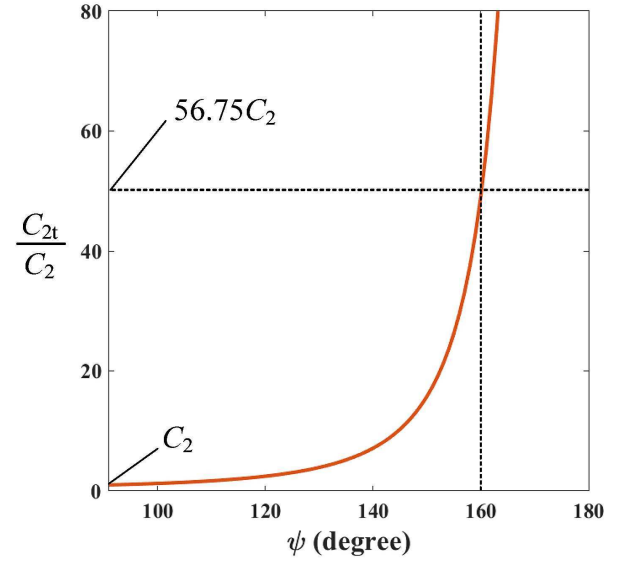


Fig. 9. Effective SCC capacitance  $C_{2t}$  versus control angle  $\psi$ .

width of the input port voltages  $V_{1j}$  as a function of the internal phase shift  $\alpha$  while maintaining a unity power factor at all ports of the immittance network. Under this control scheme,  $V_{1j}$ ,  $I_{1j}$ , and similarly for  $V_{2j}$ ,  $I_{2j}$ , are constantly in-phase with each other (cf. Fig. 7(a)) which leads to minimum RMS port currents and conduction loss. However, the major drawback of this control method is that the complimentary switches  $S_{j3}$  and  $S_{j4}$  in the lagging leg of each phase are hard switched (cf. Fig. 7(a)) leading to increased switching loss.

In order to mitigate switching loss and achieve ZVS commutation for all switches, a 2-DoF control scheme has been proposed previously that utilizes both  $\alpha$  and  $\theta$  simultaneously (*i.e.*,  $\alpha = \theta$ ) for controlling output power. By introducing  $\theta$  between  $V_{1j}$  and  $V_{2j}$ ,  $V_{1j}$  leads  $I_{1j}$  at the input ports by  $\theta$  and  $V_{2j}$  lags  $I_{2j}$  at the output ports by  $\theta$  leading to ZVS turn-on of all the switches (cf. Fig. 5(b) and Fig. 7(b)). However, the major drawback of this control method is that high circulating current and reactive power prevails under light-load conditions (cf. highlighted by the shaded areas in Fig. 7(b)) which leads to increased conduction loss. It can be safely inferred that the existing approaches cannot concurrently minimize conduction loss and switching loss in 3p-DABRIC, therefore, it is necessary to devise a new approach that can overcome this restriction for achieving a wide-range high-efficiency performance.

### B. Proposed Modulation Scheme for Tunable 3p-DABRIC

By inspection of (10), it can be observed that the switching frequency  $\omega_s$  can be used to control the output power of 3p-DABRIC. However, since  $\omega_r$  must be tuned to match with  $\omega_s$  to meet the immittance conditions (*i.e.*,  $|\mathbf{X}_A| = |\mathbf{X}_B| = X$ ), it is necessary to modulate  $\omega_r$  in synchronism with  $\omega_s$ . For modulating  $\omega_r$ , switch-controlled capacitors (SCC) are used to realize electronically tunable capacitors  $C_{2t}$  in the immittance network with tunable  $\omega_r$ . Thus, by modulating  $\omega_s$  and  $\omega_r$  concurrently (*i.e.*, dynamic frequency matching (DFM)),  $X$  can be varied and the 3p-DABRIC's output power can be controlled according to (10). The port voltages  $V_{1j}$ ,  $V_{2j}$  and port currents  $I_{1j}$ ,  $I_{2j}$  under the proposed DFM modulation are shown in Fig. 7(c)). It can be seen from Fig. 7(c) that the proposed modulation method enables a control of output power while achieving unity power factor operation and zero circulating current. Moreover, as the zero crossings of  $i_{1x}$  and  $i_{2x}$  are always aligned with the rising edges of  $v_{1x}$  and  $v_{2x}$ , all switches inherently undergo ZVS commutation. Due to the elimination of circulating current, unity power factor operation and ZVS commutation of all switches, the proposed DFM modulation scheme is anticipated to yield a wide-range high efficiency performance as will be validated by experimental results in section VI.

To derive the expression for the output power under DFM modulation and define the controllable range of  $P_o$ ,  $X$  and  $\omega_s$ , it is necessary to describe the operating principle of SCC. Recall that the switches  $S_{TxA}$ ,  $S_{TxB}$ , and capacitor  $C_2$  in branch  $\mathbf{X}_B$  constitute the SCC which acts as an electronically controllable capacitor  $C_{2t}$  as shown in Fig. 8(a). The operating principle of SCC can be explained by referring to the timing diagram for SCC as shown in Fig. 8(b) where  $i_{C_{2t}}$  is the sinusoidal current flowing through  $C_{2t}$ ,  $v_{C_{2t}}$  is the voltage across  $C_2$ ,  $\psi$  is the control angle of  $S_{TxA}$ ,  $S_{TxB}$ , and  $T$  is the period of  $i_{C_{2t}}$ . The gating signals for  $S_{TxA}$  are applied for a duration of  $\frac{\psi T}{2\pi}$  at the negative to positive zero-crossing instant of  $i_{C_{2t}}$ , while the gating signals with the same pulse-width are applied to  $S_{TxB}$  at the positive to negative zero-crossing instant of  $i_{C_{2t}}$ . At  $\omega t = \pi - \psi$ , the voltage  $v_{C_{2t}}$  across  $C_2$  is zero and  $i_{C_{2t}}$  flows from A to B through  $S_{TxA}$  and the body

diode of  $S_{TxB}$ . At  $\omega t = \psi$ ,  $S_{TxA}$  is turned off and  $i_{C_{2t}}$  flows from A to B through  $C_2$ , thus charging it for a duration of  $\frac{(\pi - \psi)T}{2\pi}$ . At the positive to negative transition of  $i_{C_{2t}}$  (*i.e.*,  $\omega t = \pi$ ),  $S_{TxB}$  is turned on,  $i_{C_{2t}}$  starts flowing in the opposite direction from B to A through  $C_2$ , thus discharging it to zero. During the next zero voltage state of  $v_{C_{2t}}$ ,  $i_{C_{2t}}$  flows from B to A through  $S_{TxB}$  and the body diode of  $S_{TxA}$  until  $S_{TxB}$  is turned off at  $\omega t = (\pi + \psi)$  followed by the charging of  $C_2$  by the negative flowing  $i_{C_{2t}}$ .

It can be inferred from Fig. 8(b) that the control angle  $\psi$  provides a means to control the charging/discharging time (*i.e.*,  $\frac{(\pi - \psi)T}{2\pi}$ ) of  $C_2$  and consequently determines the magnitude of the fundamental component of  $v_{C_{2t}}$  for a given  $i_{C_{2t}}$  [29]. By considering the fundamental component of  $v_{C_{2t}}$ , an expression for the effective capacitance  $C_{2t}$  can be derived as a function of control angle  $\psi$  as given by (11). The derivation of this expression can be found in [30]. Moreover, it should also be noted that  $S_{TxA}$  and  $S_{TxB}$  turn on and off at zero voltage and thus incur negligible commutation loss.

$$C_{2t}(\psi) = \frac{\pi C_2}{2\pi - 2\psi + \sin 2\psi} \quad (11)$$

### C. Design Considerations for Tunable 3p-DABRIC under DFM Modulation

Based on (11),  $C_{2t}$  can be varied theoretically in the range  $C_2 \leq C_{2t} \leq \infty$  corresponding to  $90^\circ \leq \psi \leq 180^\circ$ . A plot of  $C_{2t}/C_2$  versus  $\psi$  is depicted in Fig. 9 where it can be seen that  $C_{2t}$  increases non-linearly with increasing  $\psi$  and tends to  $\infty$  as  $\psi \rightarrow 180^\circ$ . To have a reasonable range of  $C_{2t}$  with good controllability, the values of  $C_{2t}$  and  $\psi$  should be restricted, for example,  $C_2 \leq C_{2t} \leq \kappa C_2$  and  $90^\circ \leq \psi \leq 160^\circ$  respectively with  $\kappa = 56.75$  (cf. Fig. 9). It should be noted that the value of  $\kappa$  is dependent on the maximum value of  $\psi$  and can be obtained from (11). Furthermore, by substituting (11) into (7), an expression for  $\omega_r$  can be obtained as a function of control angle  $\psi$  as given by (12).

$$\omega_r(\psi) = \sqrt{\frac{2\pi C_1 + \pi C_2 - 2C_1\psi + C_1 \sin(2\psi)}{\pi C_1 C_2 (L_1 + L_2)}} \quad (12)$$

By inspection of (12), it can be observed that  $\omega_r$  can be tuned to match with  $\omega_s$  by means of varying  $\psi$  in the range of  $160^\circ \geq \psi \geq 90^\circ$  to give  $\omega_{r,\min} \leq \omega_r \leq \omega_{r,\max}$ . An expression for  $\omega_{r,\min}$  and  $\omega_{r,\max}$  can be obtained by substituting the extreme values of  $\psi$  (*i.e.*,  $\psi = 160^\circ$  and  $\psi = 90^\circ$  respectively) into (12). The resulting expressions for  $\omega_{r,\min}$  and  $\omega_{r,\max}$  are given by (13).

$$\begin{aligned} \omega_{r,\min} &= \sqrt{\frac{(C_1 + \kappa C_2)}{C_1 \kappa C_2 (L_1 + L_2)}} \\ \omega_{r,\max} &= \sqrt{\frac{(C_1 + C_2)}{C_1 C_2 (L_1 + L_2)}} = \sigma \omega_{r,\min} \end{aligned} \quad (13)$$

By setting  $\theta = 0^\circ$  in (10), an expression for the output power as a function of  $\psi$  can be obtained as given by (14).

$$P_o(\psi) = \frac{3\sqrt{12}V_{in}V_o}{n\pi^2 X(\psi)} \quad (14)$$

$$\text{where } X(\psi) = \left| \omega_r L_1 - \frac{1}{\omega_r C_1} \right| = \left| \omega_r L_2 - \frac{1}{\omega_r C_{2t}} \right|.$$

By dividing (14) with  $V_o$ , the expression for the output current of 3p-DABRIC can be obtained as given by (15).

$$I_o(\psi) = \frac{3\sqrt{12}V_{in}}{n\pi^2 X(\psi)} \quad (15)$$

The expressions for  $P_{o,\min}$  and  $P_{o,\max}$  corresponding to  $\omega_{r,\min}$  and  $\omega_{r,\max}$  respectively can be obtained by substituting (13) into (14). The results are given by (16) and (17).

$$P_{o,\min} = \frac{3\sqrt{12}V_{in}V_o}{n\pi^2 X_{\max}} \quad (16)$$

where

$$X_{\max} = \left| \omega_{r,\min} L_1 - \frac{1}{\omega_{r,\min} C_1} \right| = \left| \omega_{r,\min} L_2 - \frac{1}{\omega_{r,\min} \kappa C_2} \right|.$$

$$P_{o,\max} = \frac{3\sqrt{12}V_{in}V_o}{n\pi^2 X_{\min}} = \epsilon P_{o,\min} \quad (17)$$

where

$$X_{\min} = \left| \omega_{r,\max} L_1 - \frac{1}{\omega_{r,\max} C_1} \right| = \left| \omega_{r,\max} L_2 - \frac{1}{\omega_{r,\max} \kappa C_2} \right|.$$

By inspection of (16) and (17), it can be observed that there is a certain range of output power  $P_o$  (*i.e.*, medium-to-high output power levels) that can be attained by the proposed DFM modulation. Moreover,  $P_o$  is directly proportional to  $\omega_r$  and to reduce output power,  $\omega_r$  needs to be reduced by increasing  $\psi$ . Nevertheless, below  $P_{o,\min}$  the output power cannot be reduced further by DFM modulation as  $\psi$  reaches its maximum value. Therefore, below  $P_{o,\min}$  corresponding to  $\omega_{r,\min}$ , it is proposed to reconfigure the converter to a tunable 3p-DABSRC for operation in low power range. The detailed analysis and the proposed modulation scheme for the tunable 3p-DABSRC is presented in the next section. As the desired ranges of  $P_o$  and  $\omega_r$  (cf. (12)–(17)) depend on the selection of passive component values, the key design equations for  $L_1$ ,  $L_2$ ,  $C_1$ , and  $C_2$  as a function of the multiplier terms ( $\epsilon$ ,  $\sigma$ , and  $\kappa$ ) can be obtained by simultaneously solving (16) and (17). The results are given by (18) - (21).

$$L_1 = \frac{X_{\min} \sigma (\epsilon - \sigma)}{\omega_{r,\max} (\sigma^2 - 1)} \quad (18)$$

$$L_2 = \frac{X_{\min} \sigma (\kappa \epsilon - \sigma)}{\omega_{r,\max} (\kappa - \sigma^2)} \quad (19)$$

$$C_1 = \frac{\sigma^2 - 1}{X_{\min} \omega_{r,\max} (\sigma \epsilon - 1)} \quad (20)$$

$$C_2 = \frac{\kappa - \sigma^2}{\kappa X_{\min} \omega_{r,\max} (\kappa \sigma \epsilon - 1)} \quad (21)$$

By inspection of (18)–(21), it can be observed that while selecting the values of the multiplier terms (*i.e.*,  $\epsilon$ ,  $\sigma$  and  $\kappa$ ) for a given  $P_{o,\max}$  and  $\omega_{r,\max}$ , the constraints  $\epsilon > \sigma$ ,  $\sigma > 1$ ,  $\kappa \epsilon > 1/\sigma$ , and  $\kappa > \sigma^2$  must be adhered to. Furthermore, by substituting (18)–(21) into (6), the new expressions for  $\omega_a$  and  $\omega_b$  can be obtained in terms of  $\psi$ ,  $\sigma$  and  $\kappa$  as given by (22).

$$\omega_a = \sqrt{\frac{\omega_{r,\max}^2 (\sigma \epsilon - 1)}{\sigma (\epsilon - \sigma)}} \quad (22)$$

$$\omega_b(\psi) = \sqrt{\frac{\kappa \omega_{r,\max}^2 (\sigma \epsilon - 1)}{A \sigma (\epsilon \kappa - \sigma)}}$$

$$\text{where } A = \frac{\pi}{2\pi - 2\psi + 2\sin(2\psi)}.$$

By dividing (22) with (12), the ratios  $\frac{\omega_a}{\omega_r(\psi)}$  and  $\frac{\omega_b(\psi)}{\omega_r(\psi)}$  can be obtained as given by (23). These ratios are important design parameters as they determine the voltage stress across the passive components in the immittance network and the shape of the network's current waveforms. Moreover, these ratios describe how far the branch impedances  $\mathbf{X}_A$  and  $\mathbf{X}_B$  are operating from their series-resonance frequencies  $\omega_a$  and  $\omega_b$ . A value closer to unity for these ratios implies stronger resonance, higher voltage stress and more sinusoidal current waveforms, and vice-versa.

$$\frac{\omega_a}{\omega_r(\psi)} = \sqrt{\frac{A \sigma^2 \omega_{r,\max}^2 (\sigma \epsilon - 1) (\kappa - 1)}{\sigma \omega_{r,\max}^2 (\sigma - \epsilon) (\kappa + A \sigma^2 - \sigma^2 \kappa - A \kappa)}}$$

$$\frac{\omega_b(\psi)}{\omega_r(\psi)} = \sqrt{\frac{A \sigma^2 \kappa \omega_{r,\max}^2 (\sigma \epsilon - 1) (\kappa - 1)}{A \sigma \omega_{r,\max}^2 (\sigma - \kappa \epsilon) (\kappa + A \sigma^2 - \sigma^2 \kappa - A \kappa)}} \quad (23)$$

By utilizing (18)–(23), the range of controllable output power, switching frequency, and control angle for the DFM-modulated 3p-DABRIC can be selected appropriately to have adequate voltage stress across the SCC, good sinusoidal current waveforms and a narrow switching frequency range.

## V. MODELING AND ANALYSIS OF TUNABLE 3P-DABSRC

Recall that the proposed DFM modulation scheme should not be used for modulating output power below  $P_{o,\min}$ . Although the DFM modulation scheme completely eliminates circulating current and achieves full-range ZVS operation, its implementation is constrained by the minimum allowable switching frequency. Therefore, below  $P_{o,\min}$  which is associated with  $\omega_{r,\min}$ , it is proposed to reconfigure the converter to a tunable 3p-DABSRC and use impedance modulation for modulating output power while keeping the switching frequency constant. In addition, the phase-shift  $\theta$  is kept constant

as determined by the input-to-output voltage ratio to achieve full-range ZVS operation and maintain minimum-tank-current operation simultaneously for minimizing switching loss and conduction loss respectively [29]. As a result, the proposed reconfiguration will enable high-efficiency performance for light-load operation as well.

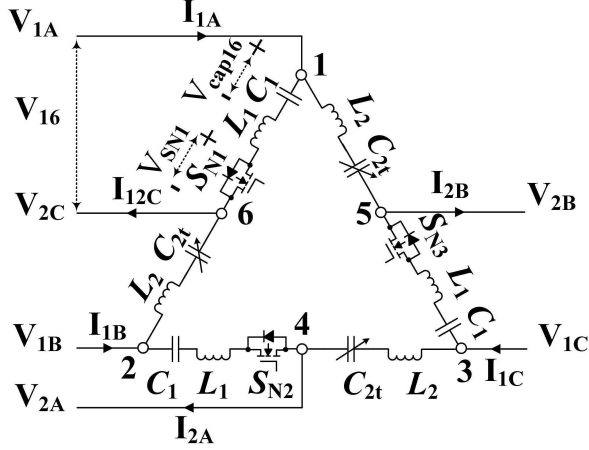


Fig. 10. Voltage distribution across branch  $X_A$ .

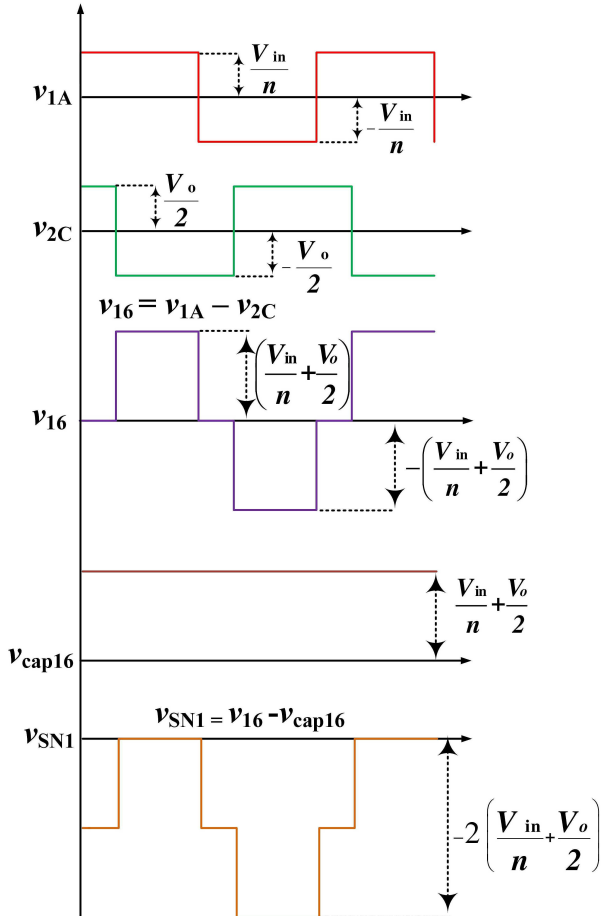


Fig. 11. Voltage waveforms across capacitor  $C_1$  and switch  $S_{N1}$  for 3p-DABSRC.

### $S_{Nx}$ Turned-off (3p-DABSRC)

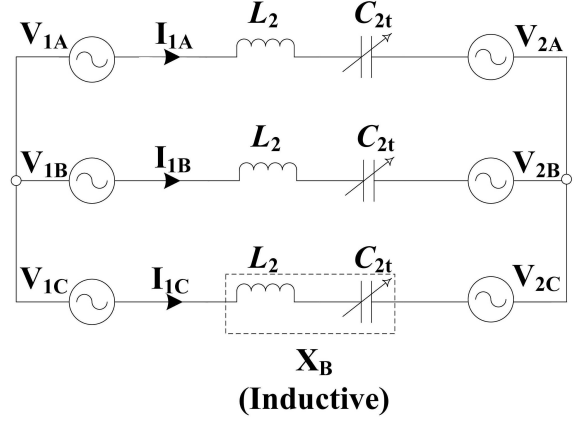


Fig. 12. Equivalent circuit of the proposed converter with 3p-TSRN.

#### A. Power Flow Analysis

For reconfiguration to a tunable 3p-DABSRC, the switches  $S_{Nx}$  in branch  $X_A$  are turned off (cf. Fig. 10). This action isolates the impedances  $X_A$  from the resonant network and only impedances  $X_B$  are active. Referring to Fig. 10, when  $S_{N1}$  (the same argument applies to  $S_{N2}$  and  $S_{N3}$ ) is turned off, the capacitor  $C_1$  in  $X_A$  is charged to the maximum voltage  $(\frac{V_{in}}{n} + \frac{V_o}{2})$  by the voltage difference  $v_{1A} - v_{2C}$  (cf. Fig. 11). As a result, the voltage seen by  $S_{N1}$ , i.e.  $v_{1A} - v_{2C} - v_{cap16}$  will vary between 0 and  $-2(\frac{V_{in}}{n} + \frac{V_o}{2})$  which will always keep the body diode of  $S_{N1}$  in a reverse biased state. Therefore,  $X_A$  can be fully isolated by using only a two-quadrant switch. Furthermore, to operate the converter as a tunable 3p-DABSRC, phase transposition must be applied at the output ports of the network i.e.,  $V_{2j}$  are to be phase-shifted by  $120^\circ$  so that the voltages at the input and output ports of the network correspond with each other. The effects of these two operations (i.e.,  $S_{Nx}$  are turned off and phase transposition is applied at the output ports), are to transform the converter to a tunable 3p-DABSRC as depicted by the equivalent circuit shown in Fig. 12. Considering the case where power is transferred from  $V_{1j}$  to  $V_{2j}$  (i.e.,  $V_{1j}$  leads  $V_{2j}$ ), the port currents  $I_{1j}$  can be obtained by applying Ohm's law as given by (24). By using (3) and (24), an expression for the output power of 3p-DABSRC can be obtained as given by (25). By dividing (25) with  $V_o$ , the expression for the output current of 3p-DABSRC can be obtained as given by (26).

$$I_{1j} = \frac{V_{1j} - V_{2j}}{X_B} = I_{1j} \angle -(\phi + f(j)\frac{2\pi}{3})$$

$$I_{1j} = \frac{\sqrt{2} V_{in} \sqrt{1 - 2G \cos(\theta) + G^2}}{n\pi X_B}$$

$$\phi = (\tan^{-1}(\frac{1 - G \cos(\theta)}{G \sin(\theta)}) \quad (24)$$

where  $G = nV_o/V_{in}$ .

$$P_o = 3 \times \Re[\mathbf{V}_{1A} \mathbf{I}_{1A}^*] \\ = \frac{6V_{in}V_o}{n\pi^2 X_B} \sin(\theta) \quad (25)$$

$$I_o = \frac{6V_{in}}{n\pi^2 X_B} \sin(\theta) \quad (26)$$

By inspection of (24), it can be observed that unlike 3p-DABRIC, the phase angle  $\phi$  of  $\mathbf{I}_{1j}$  of 3p-DABSRC is dependent on the voltage ratio  $G$ . For ZVS operation of 3p-DABSRC, the value of  $\phi$  should be in the range  $0 \leq \phi \leq \theta$  so that  $\mathbf{I}_{1j}$  lags  $\mathbf{V}_{1j}$  and leads  $\mathbf{V}_{2j}$ . This implies that the instantaneous current should be negative at the turn-on instants of the primary-side switches  $S_{j1}$  (and positive for  $S_{j2}$ ) and positive at the turn-on instants of the secondary-side switches  $Q_{j1}$  (and negative for  $Q_{j2}$ ) (cf. Fig. 3) so that the switches parasitic capacitances are discharged and their body diodes conduct before the gate signals are applied. Based on (24), when  $G$  is unity, the value of  $\phi$  will be  $\theta/2$  and 3p-DABSRC will achieve ZVS operation independent of output power. However, deviation of  $G$  from unity causes  $\phi$  to violate the constraint  $0 \leq \phi \leq \theta$  resulting in hard switching and increased circulating current. Therefore, it is proposed to calculate  $\theta$  based on the value of  $G$  such that  $\phi$  resides in the range  $0 \leq \phi \leq \theta$  and modulate output power by varying  $X_B$  (cf. 25).

### B. Criteria for ZVS and Minimum-Tank-Current Operation

Recall that for ZVS operation of all switches,  $\phi$  should reside between 0 and  $\theta$  i.e.,  $0 \leq \phi \leq \theta$ . Therefore, by evaluating  $\phi \geq 0$  and  $\phi \leq \theta$ , the ZVS conditions for the primary-side and secondary-side switches can be derived in terms of  $G$  and  $\theta$ . The results are given by (27) and (28).

$$\theta \geq \cos^{-1}(G) \quad \text{for} \quad G < 1 \quad (27)$$

$$\theta \geq \cos^{-1}\left(\frac{1}{G}\right) \quad \text{for} \quad G > 1 \quad (28)$$

Thus,  $\theta$  can be selected as a function of  $G$  according to (27)–(28) to ensure ZVS operation of the 3p-DABSRC.

To determine the criteria for minimum-tank-current operation of 3p-DABSRC, the port currents  $I_{1j}$  given by (24) are divided by the output current given by (26) to obtain a load independent and normalized expression for port current that is independent of  $X_B$ . The resulting expression is given by (29).

$$I_{N1j} = \frac{I_{1j}}{I_o} = \frac{\sqrt{2} \pi \sqrt{1 - 2G \cos(\theta) + G^2}}{6 \sin(\theta)} \quad (29)$$

By inspection of (29), it can be seen that the normalized port currents  $I_{N1j}$  are function of  $G$  and  $\theta$  only. To achieve minimum-tank-current operation, the minimum value of  $I_{N1j}$  is found by taking its first derivative (with respect to  $\theta$ ) and setting it to zero [29]. The results are given by (30).

$$\frac{d}{d\theta} (I_{N1j}) = 0 \Rightarrow \theta = \begin{cases} \cos^{-1}(G) & \forall \quad G < 1 \\ \cos^{-1}\left(\frac{1}{G}\right) & \forall \quad G > 1 \end{cases} \quad (30)$$

The conditions given by (30) (i.e.,  $\theta = \cos^{-1}(G)$  for  $G < 1$  and  $\theta = \cos^{-1}(1/G)$  for  $G > 1$ ) form the criteria for minimum-tank-current operation of 3p-DABSRC. To determine the phasor relationship between  $\mathbf{V}_{1j}$ ,  $\mathbf{V}_{2j}$  and  $\mathbf{I}_{1j}$  under minimum-tank-current operation, the values of  $\theta$  as determined by (30) are substituted in (24) to obtain  $\phi$  for different values of  $G$ . The resulting expression is given by (31).

$$\phi = \begin{cases} \theta & \forall \quad G < 1 \text{ \& } \theta = \cos^{-1}(G) \\ \frac{\theta}{2} & \forall \quad G = 1 \text{ \& } \theta = \theta_{min} \\ 0 & \forall \quad G > 1 \text{ \& } \theta = \cos^{-1}\left(\frac{1}{G}\right) \end{cases} \quad (31)$$

According to (31), under minimum-tank-current-operation,  $\mathbf{I}_{1j}$  should be in phase with  $\mathbf{V}_{2j}$  for  $G < 1$  (cf. Fig. 13 (a), (d)), and in phase with  $\mathbf{V}_{1j}$  for  $G > 1$  (cf. Fig. 13 (c), (f)). Thus, it can be concluded that by selecting  $\theta$  corresponding to (30) for different values of  $G$ , full-range ZVS and minimum-tank-current operation can be achieved for 3p-DABSRC to minimize switching loss and conduction loss simultaneously.

### C. Impedance Modulation and Design Considerations for Tunable 3p-DABSRC

After determining  $\theta$  for a given  $G$ , the output power of 3p-DABSRC can be controlled by modulating impedance  $X_B$  as a function of  $\psi$ . By substituting (30) into (25), the expression for the output power of 3p-DABSRC can be obtained as given by (32).

$$P_o(\psi) = \begin{cases} = \frac{6V_{in}V_o}{n\pi^2 X_B(\psi)} \sin(\cos^{-1}(G)) & \forall \quad G < 1 \\ = \frac{6V_{in}V_o}{n\pi^2 X_B(\psi)} \sin(\theta_{min}) & \forall \quad G = 1 \\ = \frac{6V_{in}V_o}{n\pi^2 X_B(\psi)} \sin(\cos^{-1}\left(\frac{1}{G}\right)) & \forall \quad G > 1 \end{cases} \quad (32)$$

Since the maximum output power for 3p-DABSRC is equal to the minimum output power for 3p-DABRIC, the minimum series impedance  $X_{B\text{SRC}(\min)}$  can be obtained from (16) and (32). The result is given by (33). By using (16) and (33), the switching frequency for 3p-DABSRC can be obtained as given by (34).

$$X_{B\text{SRC}(\min)} = \frac{6\epsilon V_o V_{in} \sin(\theta_{min})}{n\pi^2 P_{o,\max}} \quad (33)$$

$$\omega_s = \frac{\sqrt{(C_2 X_{B\text{SRC}(\min)})^2 + 4L_2 C_2 + C_2 X_{B\text{SRC}(\min)}}}{2L_2 C_2} \quad (34)$$

Thus, for a tunable 3p-DABSRC, the converter is operated with a constant switching frequency as given by (34) and the



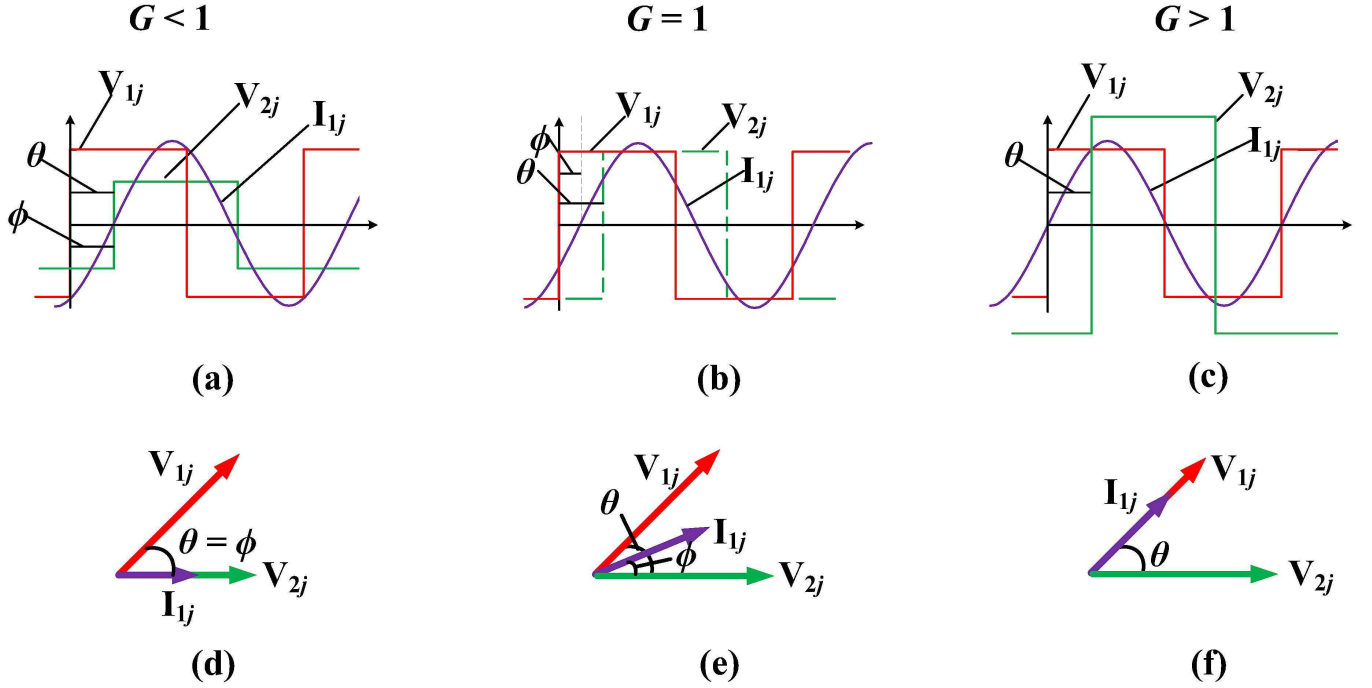


Fig. 13. Port currents and voltages of a tunable 3p-DABSRC using impedance modulation scheme for different voltage ratios  $G$  (a)  $G < 1$ ; (b)  $G = 1$ ; (c)  $G > 1$  (d) Phasor relationship between  $V_{1j}$ ,  $V_{2j}$  and  $I_{1j}$  for  $G < 1$ ; (e) Phasor relationship between  $V_{1j}$ ,  $V_{2j}$  and  $I_{1j}$  for  $G = 1$ ; and (f) Phasor relationship between  $V_{1j}$ ,  $V_{2j}$  and  $I_{1j}$  for  $G > 1$ .

output power is varied by modulating impedance  $X_B$  as a function of  $\psi$ .

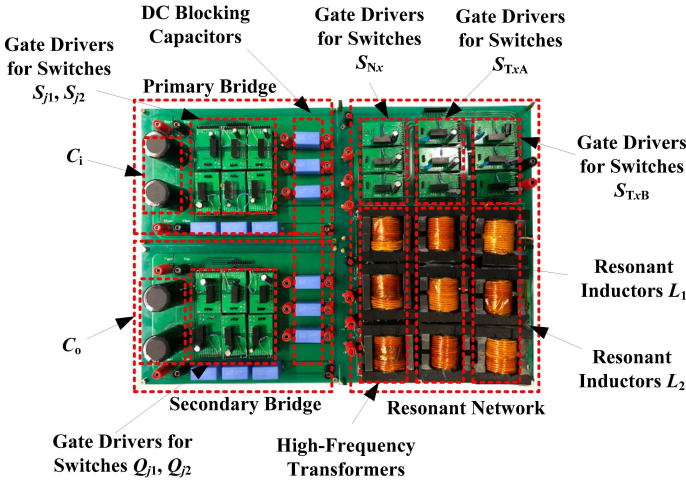


Fig. 14. Photo of constructed laboratory prototype of 1.5 kW.

## VI. PROTOTYPE DESIGN AND THE EXPERIMENTAL RESULTS

To validate the effectiveness of the proposed converter and benchmark its performance, a laboratory prototype shown in Fig. 14 has been designed and built with the specifications listed in Table I. A flowchart depicting the detailed design procedure is shown in Fig. 15. The prototype has been designed to operate as a tunable 3p-DABRIC from 40 % to 100 % of the rated output power with variable switching frequency

TABLE I  
DESIGN SPECIFICATIONS OF PROPOSED 3P-DAB CONVERTER

Input voltage $V_{in}$	300 V
Nominal output voltage $V_o$	150 V
Output voltage range	75–225 V
Maximum output power $P_{o,max}$	1500 W
Resonant inductor $L_1$	73.9 $\mu$ H
Resonant inductor $L_2$	184.4 $\mu$ H
Resonant capacitor $C_1$	81.5 nF
Resonant capacitor $C_2$	73.5 nF
Switching frequency $f_s$ (3p-DABRIC)	35–50 kHz
Switching frequency $f_s$ (3p-DABSRC)	46 kHz
Control angle of SCC $\psi$	90° – 160°
Transformer's Turns ratio $n$	2
Core Type	ETD-54 Core
Core Material	N87 Ferrite
Power gain $\epsilon$	2.5
Frequency gain $\sigma$	1.4
Capacitance gain $\kappa$	56.7
MOSFETs	UJC06505K
Controller	TMS320F28379D

such that the switching frequency varies from 35 kHz at 40 % to 50 kHz at 100% of the rated output power. The variation of  $f_s$  and  $\psi$  versus  $P_o$  is plotted in Fig. 16a by using (14). It can be seen from Fig. 16a that  $P_o$  varies from 40 % to 100 % of the rated output power as  $f_s$  varies from 35 kHz to 50 kHz and  $\psi$  varies from 160° to 90°. It is reminded that under the proposed DFM modulation for 3p-DABRIC,  $f_s$

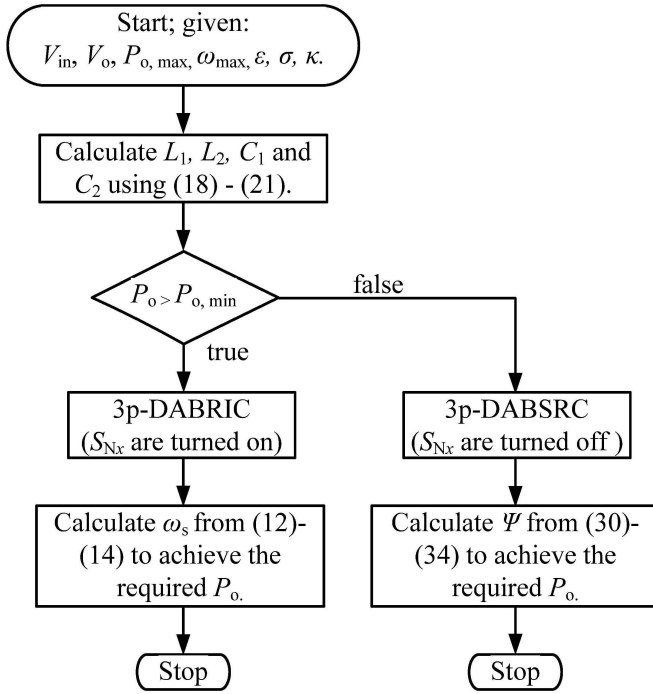


Fig. 15. Flowchart for design and operation of proposed converter.

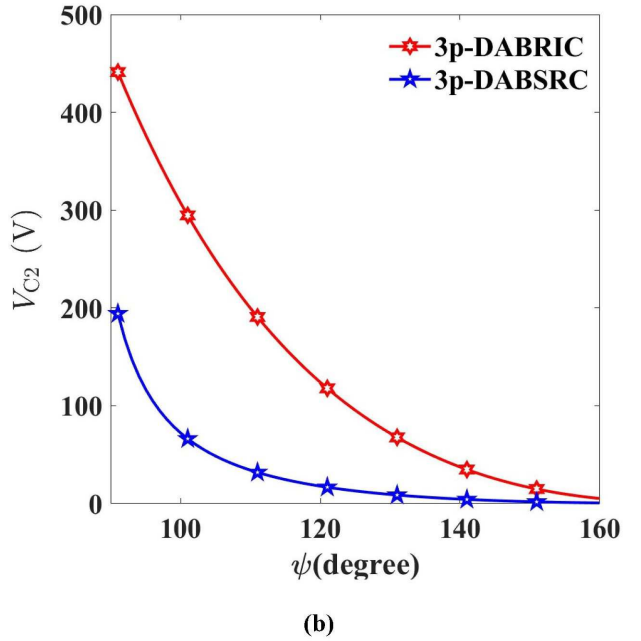
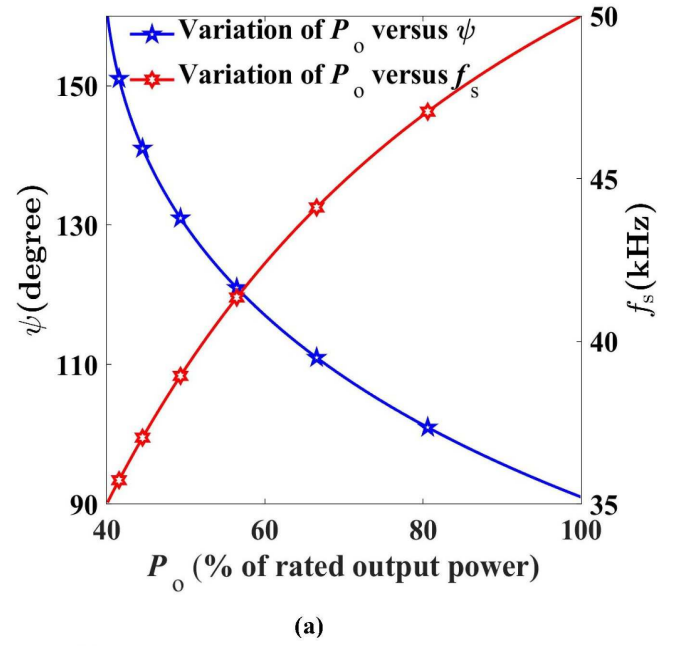
and  $\psi$  are varied synchronously to maintain the immittance condition (*i.e.*,  $\omega_r = \omega_s$ ). Below 40 % of the rated output power (*i.e.*,  $P_o \leq P_{o,max}/\sigma$ ), the converter is reconfigured to a tunable 3p-DABSRC. The output power for the tunable 3p-DABSRC is controlled by impedance modulation method where the impedance of the series  $LC$  network is modulated by varying  $\psi$  from  $90^\circ$  to  $160^\circ$ . Regarding the selection of the multiplier terms  $\epsilon$ ,  $\sigma$ , and  $\kappa$  they are selected to limit the voltage stress across the SCC and keep a narrow range of frequency variation. The voltage stress across the SCC for the selected converter specifications is plotted using (35) and is shown in Fig. 16b. The derivation of this expression can be found in [29].

$$V_{C2,peak} = \frac{\sqrt{2}I_{C2}}{\omega C_{2t}} = \frac{\sqrt{2}(2\pi - 2\psi + \sin 2\psi)I_{C2}}{\omega\pi C_2} \quad (35)$$

To characterize the performance of the proposed converter, the constructed prototype has been extensively tested under both configurations and its power conversion efficiency has been measured over a wide range of  $G$  (*i.e.*,  $0.5 \leq G \leq 1.5$ ). Moreover, for better benchmarking its efficiency performance, the port currents and efficiency of the proposed converter has been compared to conventional 3p-DABRIC operating in UPF and ZVS modes and 3p-DABSRC under SPS modulation.

#### A. Experimental Results for Tunable 3p-DABRIC under DFM Modulation

The prototype was operated as a tunable 3p-DABRIC from 40 % to 100 % of the rated output power with the proposed DFM modulation, under which  $f_s$  and  $\psi$  were modulated simultaneously to maintain the immittance condition (*i.e.*,  $\omega_r = \omega_s$ ) through-out the entire power range. The corre-

Fig. 16. (a) Variation of  $f_s$  and  $\psi$  versus  $P_o$  for 3p-DABRIC; (b) Variation of  $V_{C2}$  versus  $\psi$  for 3p-DABRIC and 3p-DABSRC.

sponding experimental results are shown in Fig. 17 for three different power levels *i.e.*, 100 %, 75 % and 50 % of the rated output power. By inspection of the experimental waveforms shown in Fig. 17, it can be observed that  $v_{1j}$ ,  $i_{1j}$  at the input ports (cf. Fig. 17 (a), (c) and (e)) and  $v_{2j}$ ,  $i_{2j}$  at the output ports (cf. Fig. 17 (b), (d) and (f)) maintain an in-phase relationship for all the three output power levels. Moreover, it can also be observed that as the immittance condition is maintained under switching frequency variation, circulating current and reactive power are completely eliminated. The in-phase relationship between the current and voltage waveforms at all ports and the elimination of circulating current have

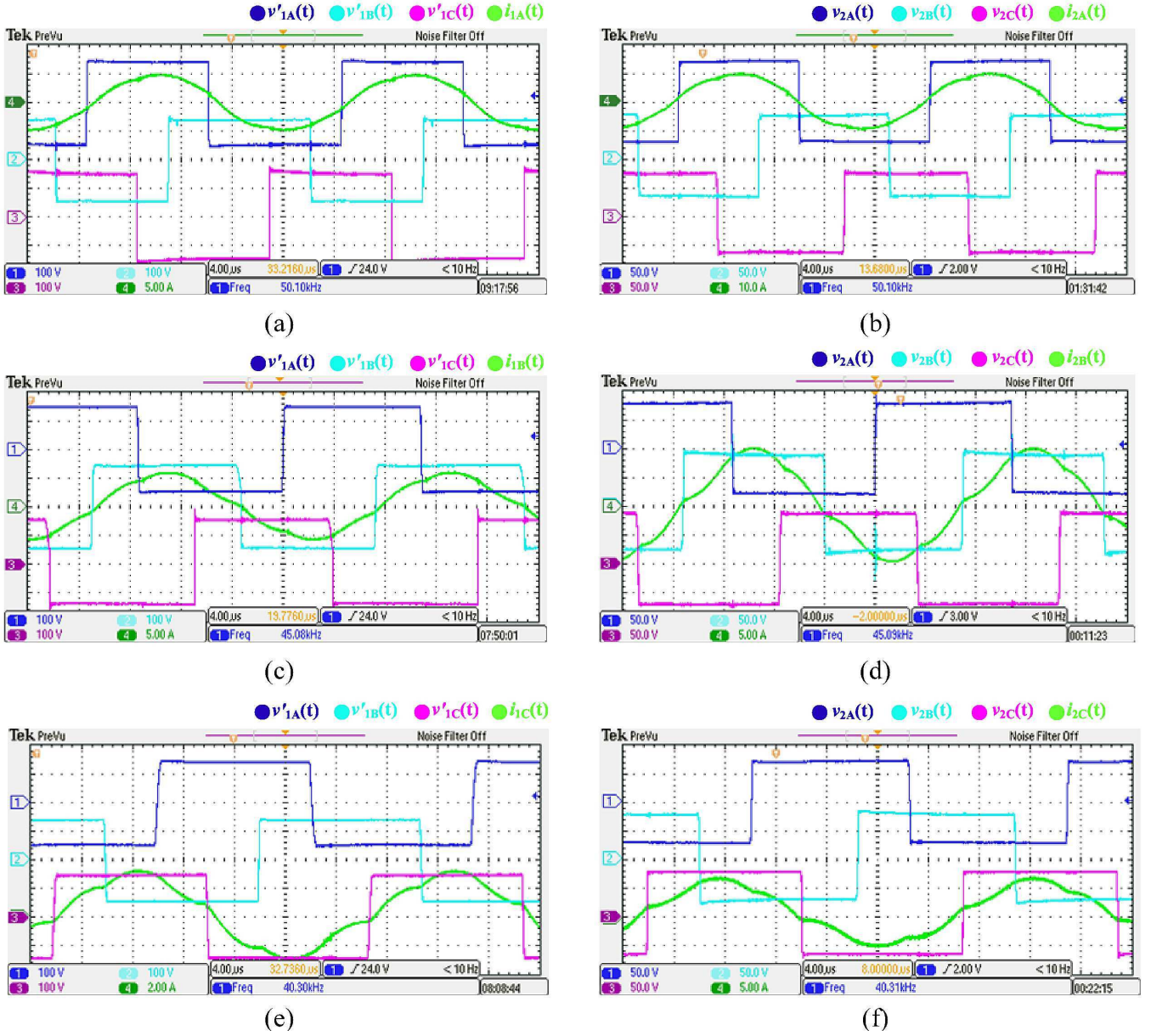


Fig. 17. Measured port voltages  $v'_{1j}$ ,  $v_{2j}$  and currents  $i_{1j}$ ,  $i_{2j}$  for 3p-DABRIC under DFM at (a)–(b) 100% of the rated output power with  $f_s = 50$  kHz, and  $\psi = 90^\circ$ ; (c)–(d) 75% of the rated output power with  $f_s = 45$  kHz, and  $\psi = 104.7^\circ$ ; (e)–(f) 50% of the rated output power with  $f_s = 40$  kHz, and  $\psi = 123.9^\circ$ .

led to significantly reduced RMS port currents  $i_{1j}$ ,  $i_{2j}$  and conduction loss. Furthermore, judging from the direction of  $i_{1j}$ ,  $i_{2j}$  during the voltage transitions of  $v_{1j}$ ,  $v_{2j}$ , it can be observed that all switches undergo ZVS commutation leading to mitigation of switching loss. Overall, the elimination of circulating current and achievement of ZVS operation in all switches have contributed to the achievement of wide-range high-efficiency performance.

#### B. Experimental Results for Tunable 3p-DABSRC Under Impedance Modulation

Below 40% of the rated output power, the prototype was operated as a tunable 3p-DABSRC with impedance modulation. Under this modulation method, switching frequency  $f_s$

and phase-shift  $\theta$  were kept constant and the output power was controlled by modulating the impedance of the series  $LC$  resonant network by means of varying the SCC control angle  $\psi$ . The corresponding experimental are shown in Fig. 18 corresponding to three different output power levels *i.e.*, 40 %, 25 %, and 10 % of the rated output power. By inspection of Fig. 18, it can be observed that unlike SPS modulation where circulating current increases with the increase of  $\theta$ , the proposed modulation method constantly operates the converter at the minimum tank current for the entire power range as  $\theta$  is kept constant at the optimal value as determined from (30), and  $\psi$  is used to modulate output power. Moreover, it can be observed from Fig. 18 that  $i_{1j}$  lags  $v_{1j}$  and leads  $v_{2j}$  leading to the ZVS operation of all switches. The waveforms



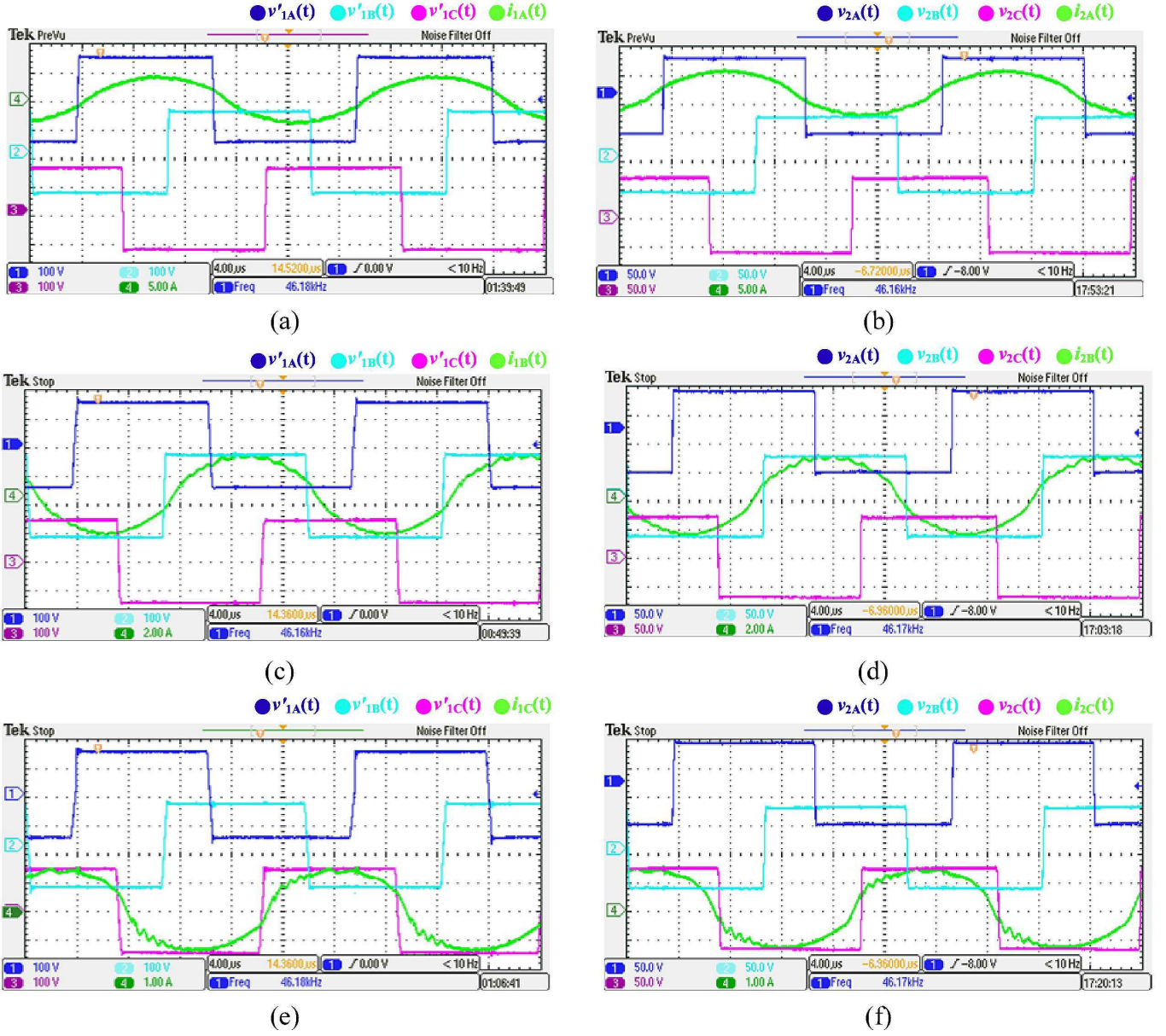


Fig. 18. Measured port voltages  $v'_{1j}$ ,  $v_{2j}$  and currents  $i_{1j}$ ,  $i_{2j}$  for 3p-DABSRC under impedance modulation at (a)–(b) 40% of the rated output power with  $\psi = 90^\circ$ ; (c)–(d) 25% of the rated output power with  $\psi = 95.8^\circ$ ; (e)–(f) 10% of the rated output power with  $\psi = 105.7^\circ$ .

depicting the ZVS operation of the primary-side switches  $S_{A1}$ ,  $S_{A2}$  and secondary-side switches  $Q_{A1}$ ,  $Q_{A2}$  are shown in Fig. 19(a)–(b) respectively, and the voltage waveforms of SCC are shown in Fig. 19(c). Hence, it can be concluded that under this operation mode, attenuation of the circulating current in conjunction with ZVS operation of all switches have contributed to the realization of high-efficiency performance at low-to-medium output power levels.

### C. Closed-Loop Voltage-Mode Control Transient Performance Under Mode Switching

To demonstrate the transient performance of the proposed converter under fast load transitions and mode switching, the simulated closed-loop transient response of the proposed converter with voltage-mode control under step load changes

of  $1A \rightarrow 7.5A \rightarrow 1A$  are shown in Fig. 20. The converter is designed to operate as a tunable 3p-DABSRC for load current below 4 A and a tunable 3p-DABRIC for load current above 4A, the load current is sensed continuously and mode transition occurs seamlessly as the load current exceeds/fall below the threshold value of 4A. The block diagram representing the closed-loop voltage-mode control strategy of the proposed converter is depicted in Fig. 21. The feedback loop comprises of a mode selection block which selects between two different proportional-integral (PI) controllers (designed for each operation mode) on the basis of the load current, and a compensation block which contains the two PI controllers that generate the appropriate control signals for output voltage regulation under each operation mode. A hysteresis band is designed in the mode selection block to reduce sensitivity at the threshold

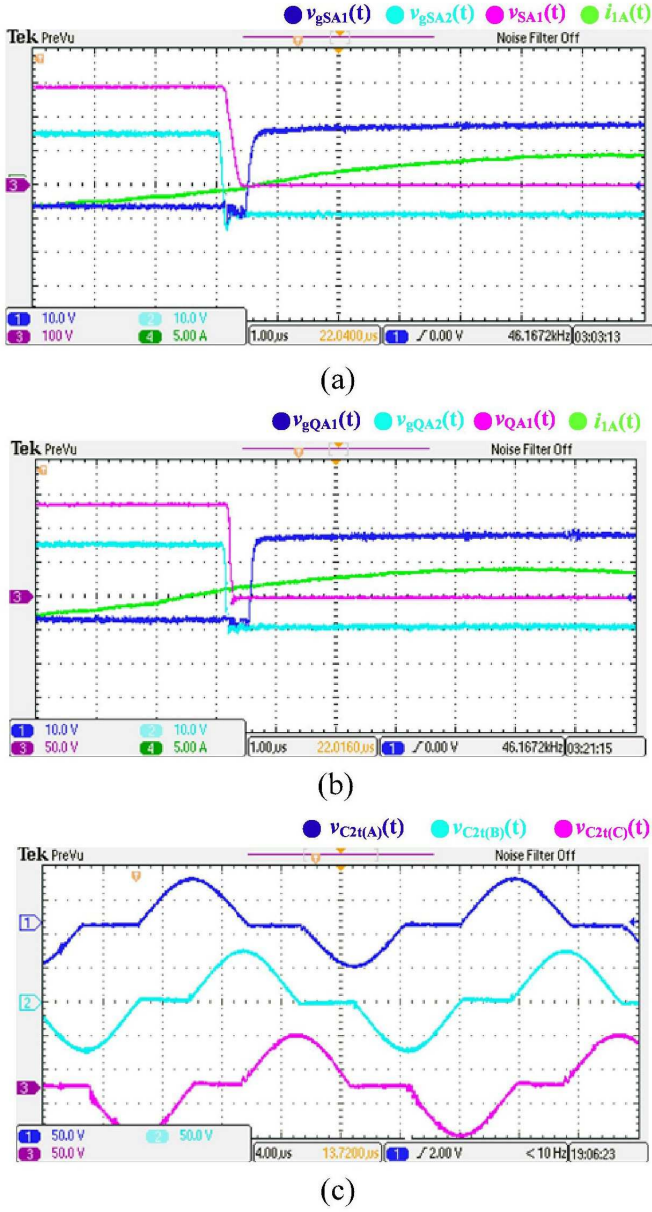


Fig. 19. (a) ZVS operation of  $S_{A1}$ ,  $S_{A2}$ ; (b) ZVS operation of  $Q_{A1}$ ,  $Q_{A2}$ ; (c) Measured waveforms of  $v_{Ct(A)}$ ,  $v_{Ct(B)}$  and  $v_{Ct(C)}$  at  $\psi = 115.3^\circ$ .

value of 4 A. Based on the simplified small-signal model of the proposed converter as shown in Fig. 22, the PI controllers are designed with a crossover frequency of 500 Hz and a phase margin of  $70^\circ$ . The expressions for the small-signal value of the output current  $i_{o\_mode, avg}$  for 3p-DABRIC and 3p-DABSRC can be obtained by taking the partial derivative of (14) and (26) with respect to  $f_s$  and  $\psi$ , respectively. The resulting expressions are given by (36) and (37). For tunable 3p-DABRIC, as  $f_s$  is varied, the corresponding value of  $\psi$  required to maintain the immittance condition is taken from a look-up-table. The Bode plots of the compensated loop gain for each operation mode are shown in Fig. 23. It can be seen from Fig. 20 and Fig. 23 that with suitably designed PI controllers, the converter's output voltage is well regulated during fast load transients and the converter is able to operate

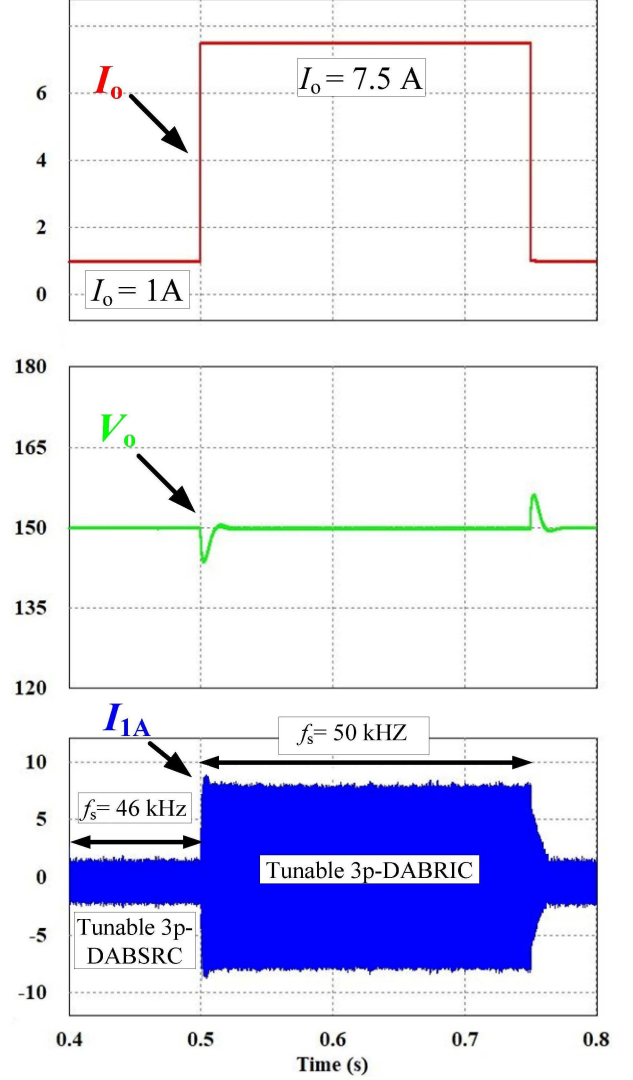


Fig. 20. Closed-loop transient response under step load changes of 1A→7.5A→1A accompanied by mode transitions between 3p-DABSRC and 3p-DABRIC.

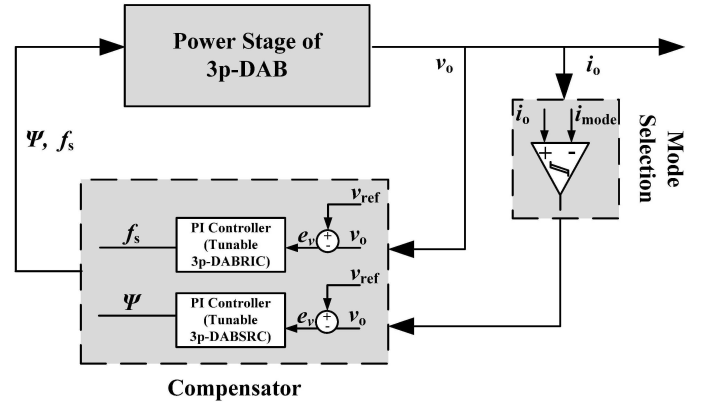


Fig. 21. Block diagram of the closed-loop implementation of the proposed converter.

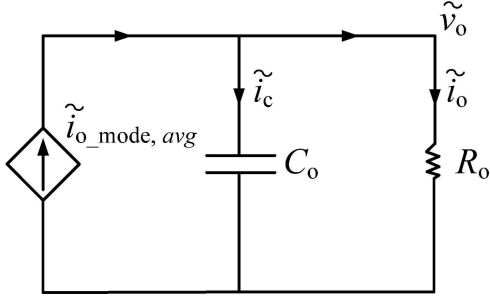


Fig. 22. Simplified small-signal model of the proposed converter.

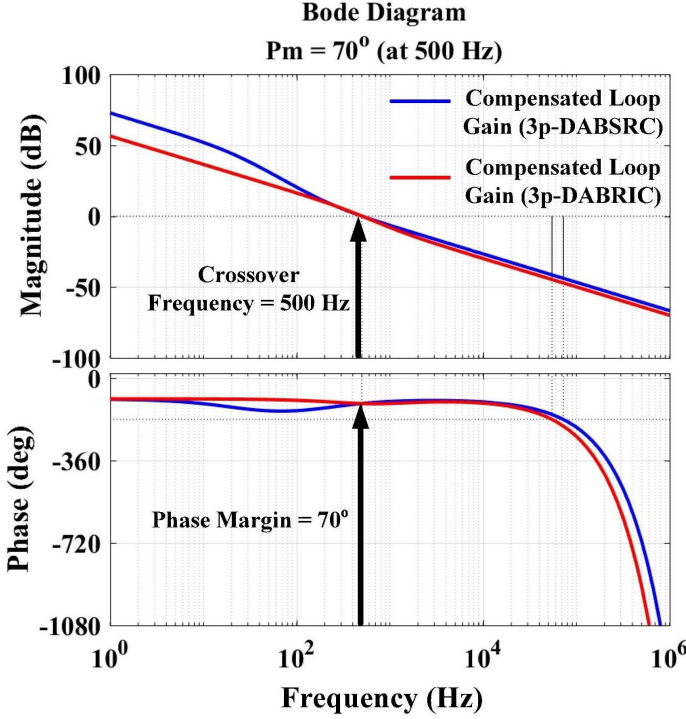


Fig. 23. Bode plots of the compensated loop gain for each operation mode.

stably during mode transition.

$$\tilde{i}_{o\_3p-DABRIC,avg} = \frac{12\sqrt{3}C_1V_{in}(4\pi^2C_1L_1f_s^2 + 1)}{n\pi(4\pi^2C_1L_1f_s^2 - 1)^2} \tilde{f}_s \quad (36)$$

$$\tilde{i}_{o\_3p-DABSRC,avg} = \frac{48C_2V_{in}f_s \sin(\theta)(\cos(\psi)^2 - 1)}{n(2\pi - 4\pi^3C_2L_2f_s^2 - 2\psi + \sin(2\psi))^2} \tilde{\psi} \quad (37)$$

#### D. Performance Comparison with Conventional 3p-DABRIC and 3p-DABSRC

To demonstrate the merits of the proposed topology and modulation schemes, the performance of the proposed converter was evaluated and compared (in terms of RMS port currents, ZVS range and power conversion efficiency) with the conventional 3p-DABRIC and 3p-DABSRC with fixed resonant networks. The conventional 3p-DABRIC with a fixed immittance network operating with unity power factor and full-range ZVS modes, and 3p-DABSRC operating with SPS

modulation were included in the comparison. For a more comprehensive comparison, wide-range variations in  $G$  (i.e.,  $0.5 \leq G \leq 1.5$ ) are considered. The simulated RMS port currents (i.e.,  $I_{N,rms} = (I_{1j} + I_{2j})/2$ ) of the proposed converter, conventional 3p-DABRIC and 3p-DABSRC for the selected converter specifications are plotted in Fig. 24(a)–(c) for  $G = 0.5$ ,  $G = 1$  and  $G = 1.5$  respectively. By inspection of Fig. 24(a)–(c), it can be observed that the proposed converter offers significantly lower RMS port currents under wide-range variations in  $G$  as compared to the other two topologies. The lower RMS port currents of the proposed converter is attributed to the achievement of wide-range zero circulating current which favorably leads to lower conduction loss. Moreover, as the proposed converter is capable of achieving full-range ZVS operation for all switches irrespective of variations in  $G$ , the proposed converter incurs lower switching loss in comparison to the other two topologies. The ZVS range comparison of the proposed converter with the other two topologies is presented in Fig. 25. It can be seen from Fig. 25 that unlike UPF operation of 3p-DABRIC (cf. Fig. 25(a) where switches  $S_{j3}$ ,  $S_{j4}$  are hard-switched) and SPS operation of 3p-DABSRC (cf. Fig. 25(b) where switches  $Q_{j1}$ ,  $Q_{j2}$  are hard-switched for  $G < 1$  and switches  $S_{j1}$ ,  $S_{j2}$  are hard-switched for  $G > 1$ ), the proposed converter achieves ZVS operation for all the switches independent of output power level and input-to-output voltage ratio (cf. Fig. 25(c)).

The measured power conversion efficiency of the proposed converter, conventional 3p-DABRIC and 3p-DABSRC are plotted in Figs. 26(a)–(c) for  $G = 0.5$ ,  $G = 1$  and  $G = 1.5$  respectively. It can be observed from Figs. 26(a)–(c) that due to reconfiguration flexibility, attenuation of circulating current and full-range ZVS operation, the proposed converter offers a wide-range high-efficiency performance as compared to the other two topologies. For the conventional 3p-DABSRC, it achieved a better efficiency performance at low-to-medium output power levels for  $G = 1$  but suffered from high circulating current and hard-switching under  $G \neq 1$  leading to a deterioration of efficiency performance. For the conventional 3p-DABRIC operating with unity power factor, 33% of the switches suffered from hard-switching and high circulating current prevails under light-load condition. The worst efficiency performance resulted from the conventional 3p-DABRIC operating with full-range ZVS mode due to excessive conduction loss caused by the high circulating current needed to realize the ZVS operation. For the proposed converter, switching loss is negligible due to full-range ZVS operation of all switches and the dominant loss mechanism is conduction loss. The increase in conduction loss with increasing load leads to a decrease in efficiency. However due to wide-range zero circulating current, unity power factor operation and ZVS operation of all switches, the loss in efficiency with increasing load is quite small and the proposed converter offers relatively flat (i.e.; weak dependence on load) efficiency curves as compared to the other two topologies.

#### E. Power Loss Model

The power loss breakdown of the proposed converter for both operation modes over wide output voltage and power



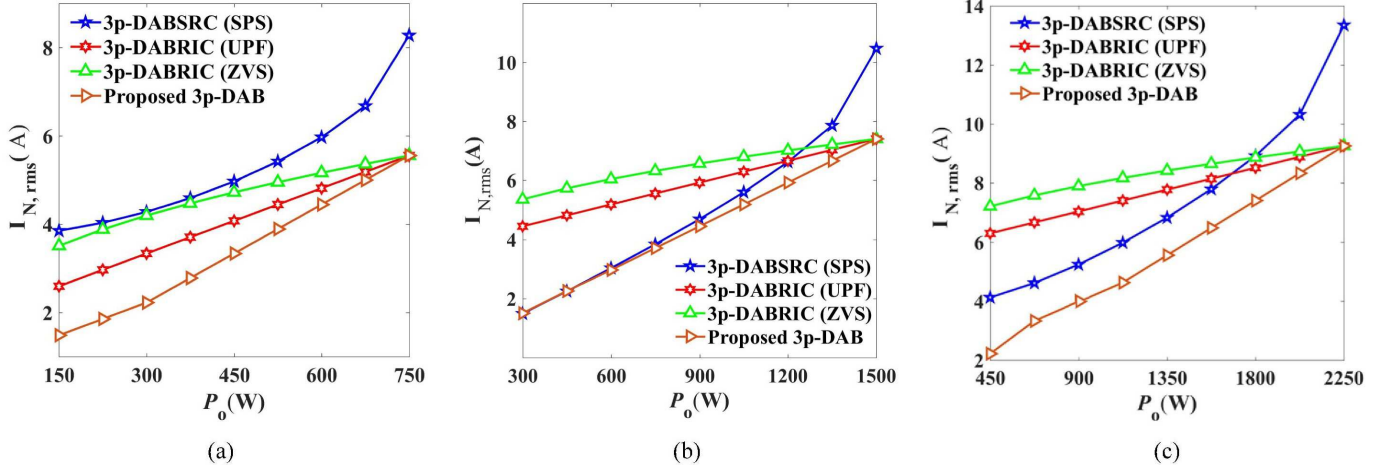


Fig. 24. Normalized simulated port currents  $I_{N,rms}$  comparison between the proposed converter and conventional 3p-DABRIC (UPF and ZVS modes) and 3p-DABSRC (SPS modulation) for (a)  $G = 0.5$ ; (b)  $G = 1$ ; (c)  $G = 1.5$ .

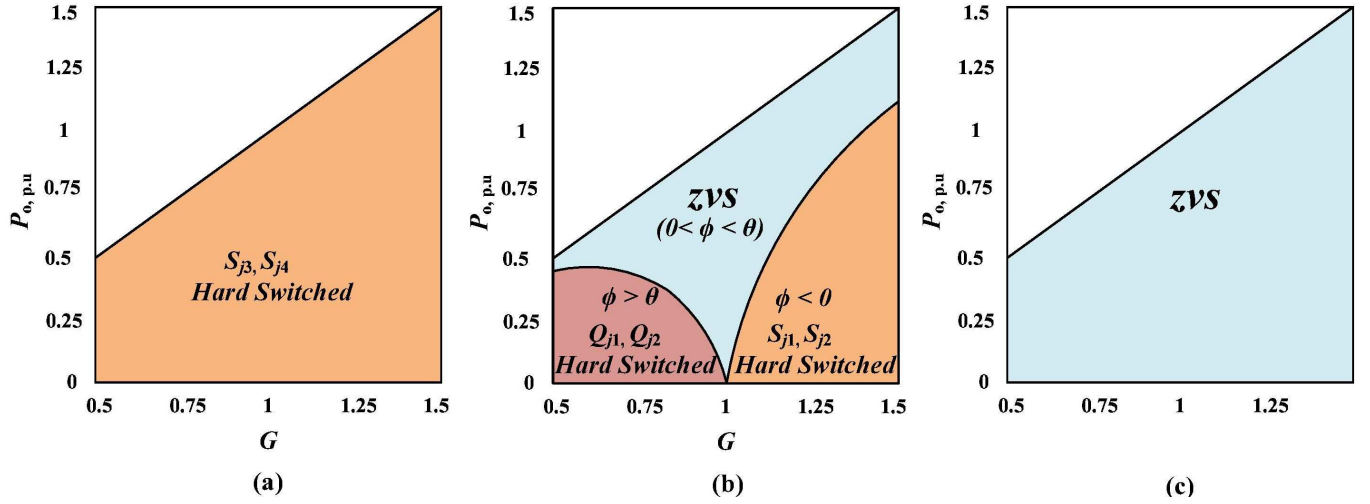


Fig. 25. (a) ZVS range of 3p-DABRIC (UPF mode); (b) ZVS range of SPS controlled 3p-DABSRC; (c) ZVS range of the proposed converter.

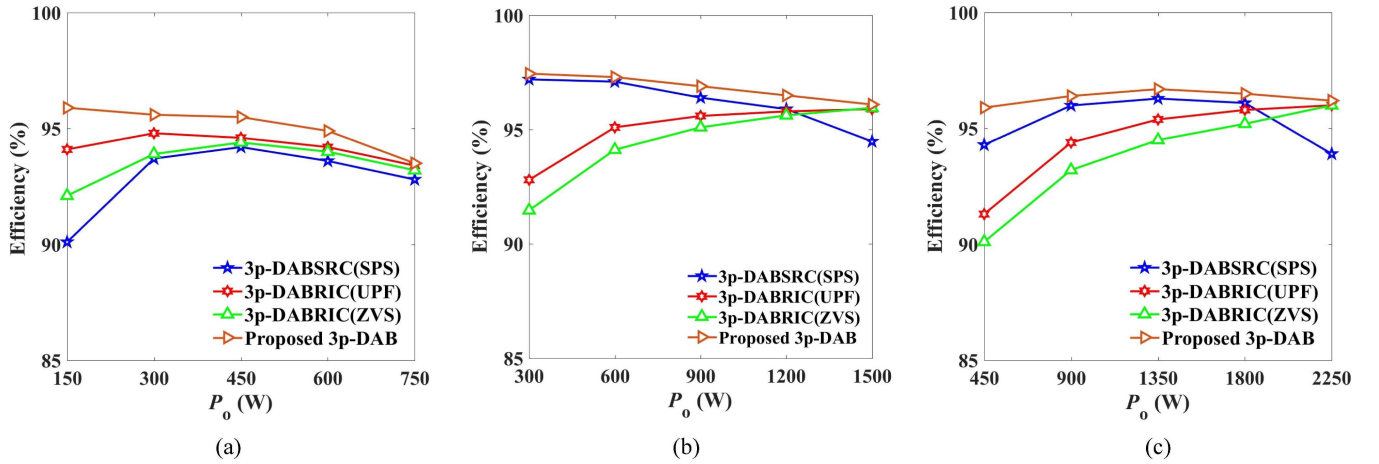


Fig. 26. Comparison of measured power conversion efficiency between the proposed converter and conventional 3p-DABRIC (UPF and ZVS modes) and 3p-DABSRC (SPS modulation) for (a)  $G = 0.5$ ; (b)  $G = 1$ ; (c)  $G = 1.5$ .

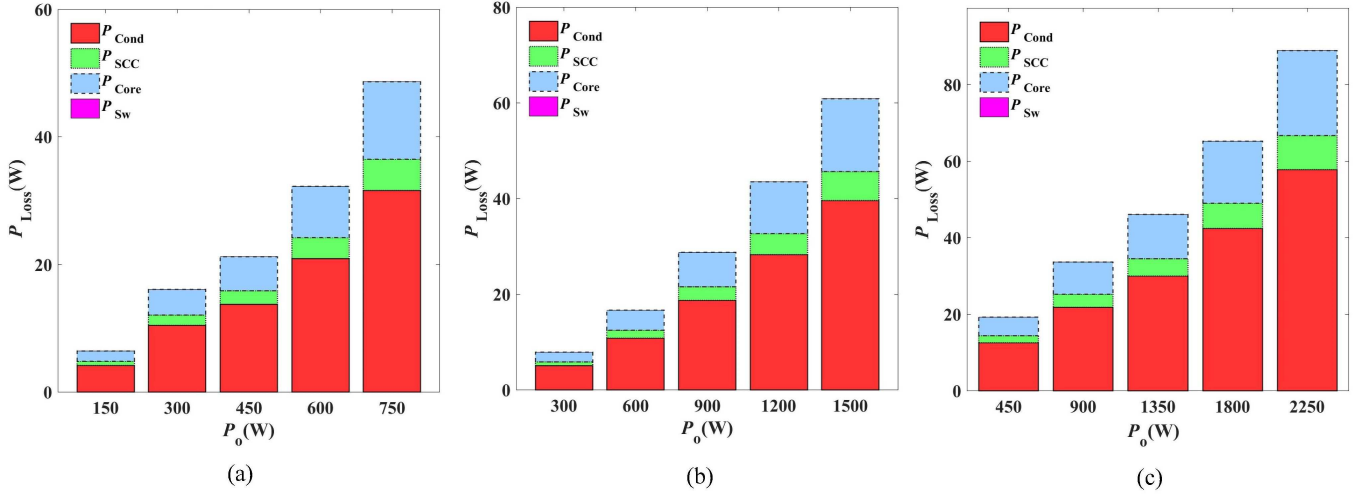


Fig. 27. Power loss breakdown of the proposed converter for (a)  $G = 0.5$ ; (b)  $G = 1$ ; (c)  $G = 1.5$ .

range has been calculated by adopting and extending the power loss models proposed in [31], [32]. The developed power loss model (cf. (38)) considers the following losses:

$$P_L = P_{\text{Cond}} + P_{\text{SCC}} + P_{\text{Core}} + P_{\text{Sw}} \quad (38)$$

1) *Conduction Loss*: The conduction loss  $P_{\text{Cond}}$  is calculated using (39) and is contributed by the on-state resistance  $R_{\text{ON,p}}$ ,  $R_{\text{ON,s}}$  of the MOSFETs in the primary and secondary bridges respectively, AC resistances  $R_{L1}$ ,  $R_{L2}$  of the resonant inductors  $L_1$ ,  $L_2$  respectively, and AC resistance  $R_T$  (secondary-reflected) of the HF transformers windings.

$$P_{\text{cond}} = 6 \times \left( \left( \frac{I_{1j,\text{rms}}}{n\sqrt{2}} \right)^2 R_{\text{ON,p}} + \left( \frac{I_{2j,\text{rms}}}{\sqrt{2}} \right)^2 R_{\text{ON,s}} \right) + 3 \times \left( (I_{L1,\text{rms}})^2 R_{L1} + (I_{L2,\text{rms}})^2 R_{L2} + (I_{2j,\text{rms}})^2 R_T \right) \quad (39)$$

2) *Power Loss in SCC*: The power loss in SCC is calculated using (40) and is contributed by the on-state resistance  $R_{\text{ON,SCC}}$  of the SCC MOSFETs and the forward voltage drop  $V_f$  of the SCC body diodes [29].

$$P_{\text{SCC}} = 3 \times \left( (I_{\text{SCC,rms}})^2 R_{\text{ON,SCC}} + V_f I_{\text{SCC,rms}} \right) \quad (40)$$

3) *Core Loss*: The total core loss incurred in the resonant inductors  $L_1$ ,  $L_2$  and HF transformers is calculated by applying the modified Steinmetz equation as given by (41).

$$P_{\text{core}} = 3 \times \left( K_{fe} A_c l_m \left( \Delta B_T^\beta + \Delta B_{L1}^\beta + \Delta B_{L2}^\beta \right) \right) \quad (41)$$

where  $K_{fe}$ ,  $A_c l_m$ , and  $\beta$  are the loss coefficient, volume, and loss exponent of the magnetic core respectively, while  $\Delta B_T$ ,  $\Delta B_{L1}$ , and  $\Delta B_{L2}$  are the flux densities of the HF transformer, resonant inductor  $L_1$ , and resonant inductor  $L_2$ , respectively.

4) *Switching Loss*: The total switching loss is given by (42) and is caused by the voltage and current overlap during the

switching transitions of MOSFETs.

$$P_{\text{Sw}} = \left( 6 \times \left| \frac{i_{1j}(0)}{n} \right| V'_{1j} (t_{\text{rip}} + t_{\text{rvp}} + t_{\text{fip}} + t_{\text{rvp}}) f_s \right) + \left( 6 \times |i_{2j}(\theta)| V_{2j} (t_{\text{ris}} + t_{\text{rvs}} + t_{\text{fis}} + t_{\text{rvs}}) f_s \right) \quad (42)$$

where  $\left| \frac{i_{1j}(0)}{n} \right|$ ,  $|i_{2j}(\theta)|$  are the instantaneous current values at the turn-on instants of primary and secondary-bridge MOSFETs,  $t_{\text{rip}}$ ,  $t_{\text{rvp}}$ ,  $t_{\text{fip}}$ , and  $t_{\text{rvp}}$  are the rise and fall time of the current and voltage of the primary-bridge MOSFETs while  $t_{\text{ris}}$ ,  $t_{\text{rvs}}$ ,  $t_{\text{fis}}$ , and  $t_{\text{rvs}}$  are the same for secondary-bridge MOSFETs.

By using (38)-(42), the power loss breakdown of the proposed converter is calculated and shown in Figs. 27(a)–(c) for  $G = 0.5$ ,  $G = 1$ , and  $G = 1.5$ , respectively. Since ZVS operation is achieved for the entire operating range, the switching loss  $P_{\text{Sw}}$  of the MOSFETs in the primary and secondary bridges is negligible and is not shown in Fig. 27. It can be seen from Fig. 27 that the major source of power loss of the proposed converter is attributed to conduction loss for the entire operating range.

Finally, a comprehensive comparison of the proposed 3p-DAB topology with the existing 3p-DAB topologies is presented in Table II to conclude the paper.

## VII. CONCLUSION

A flexible and reconfigurable topology of three-phase DAB converter utilizing a reconfigurable and tunable resonant network has been proposed in this paper. The proposed converter offers multiple degrees-of-freedom in terms of the availability of multiple control parameters and topological variations to achieve wide-range high efficiency performance. The proposed converter has been designed to switch between two different network configurations that are controlled with two different modulation schemes to optimize its efficiency performance in both low and medium-to-high power operation. For medium-to-high output power levels, the converter operates as a tunable 3p-DABRIC with dynamic frequency matching modulation.



TABLE II  
COMPARISON BETWEEN THE PROPOSED TOPOLOGY AND EXISTING TOPOLOGIES.

S. No.	Ref	No. of Switches	Additional Resonant Components	Full-Range ZVS	Unity Power Factor Operation	Zero Circulating Current	Zero Backflow Power	Minimum Tank Current
1	[11]–[13]	24	0	✓	X	X	X	X
2	[15], [16]	21	0	✓	X	X	X	X
3	[19], [20]	12	9	✓	X	X	X	X
4	[21]	18	12	X	✓	X	✓	X
5	[26]	21	12	✓	✓	X	✓	X
6	Proposed Topology	21	12	✓	✓	✓	✓	✓

Under this mode of operation, the converter's output power is modulated by synchronously varying the resonance frequency of the immittance network and the switching frequency by using an SCC. For low-to-medium output power levels, the converter operates as a tunable 3p-DABSRC with impedance modulation. Under this mode of operation, the converter's output power is modulated by varying the impedance of a series  $LC$  resonant network with the aid of SCC while keeping the switching frequency and phase-shift constant. The combination of both operation modes have led to the realization of wide-range zero circulating current and full-range ZVS operation of all switches for a wide-range of output power and input-to-output voltage ratio. The presented theoretical analysis has been validated by the experimental results obtained from a 1.5 kW laboratory prototype yielding wide-range high-efficiency performance.

#### ACKNOWLEDGMENT

This work was supported by The Hong Kong Polytechnic University Central Research under Grant G-YBXL.

#### REFERENCES

- [1] R. W. A. A. De Doncker, D. M. Divan and M. H. K. Kheraluwala, "A three-phase soft-switched high-power-density DC/DC converter for high-power applications," *IEEE Trans. Ind. Appl.*, vol. 27, no. 1, pp. 63–73, 1991.
- [2] Z. Wang, S. Member, H. Li, and S. Member, "A Soft Switching Three-phase Current-fed Bidirectional DC-DC Converter With High Efficiency Over a Wide Input Voltage Range," *IEEE Trans. Power Electron.*, vol. 27, no. 2, pp. 669–684, 2012.
- [3] J. Huang, Y. Wang, Z. Li, Y. Jiang, and W. Lei, "Simultaneous PWM control to operate the three-phase dual active bridge converter under soft switching in the whole load range," *Conference Proceedings - IEEE Applied Power Electronics Conference and Exposition - APEC*, vol. 2015-May, no. May, pp. 2885–2891, 2015.
- [4] J. Hu, Z. Yang, N. Soltan, and R. W. A. A. De Doncker, "A Duty-Cycle Control Method to Ensure Soft-Switching Operation of a High-Power Three-Phase Dual-Active Bridge Converter," in *2017 IEEE 3rd International Future Energy Electronics Conference and ECCE Asia (IFEEC 2017 - ECCE Asia)*, no. Dec, 2017, pp. 1–6.
- [5] H.-J. Choi, H.-P. Park, M.-A. Kim, C. Sang-Gyu, C.-U. Lee, and J.-H. Jung, "Modulation Strategy of Three-Phase Dual-Active-Bridge Converter Using SiC-MOSFET for Improving Light Load Condition," *2019 IEEE Workshop on Wide Bandgap Power Devices and Applications in Asia (WiPDA Asia)*, pp. 1–5, 2019.
- [6] N. A. Dung, H. J. Chiu, J. Y. Lin, Y. C. Hsieh, H. T. Chen, and B. X. Zeng, "Novel Modulation of Isolated Bidirectional DC-DC Converter for Energy Storage Systems," *IEEE Trans. Power Electron.*, vol. 34, no. 2, pp. 1266–1275, 2019.
- [7] J. Hiltunen and V. Vesa, "Variable-Frequency Phase Shift Modulation of a Dual Active Bridge Converter," *IEEE Trans. Power Electron.*, vol. 30, no. 12, pp. 7138–7148, 2015.
- [8] Z. Li, Y. Wang, L. Shi, J. Huang, and W. Lei, "Optimized modulation strategy for three-phase dual-active-bridge DC-DC converters to minimize RMS inductor current in the whole load range," *2016 IEEE 8th International Power Electronics and Motion Control Conference, IPEMC-ECCE Asia 2016*, pp. 2787–2791, 2016.
- [9] J. Hu, N. Soltan, and R. W. A. A. De Doncker, "Asymmetrical Duty-Cycle Control of Three-Phase Dual-Active Bridge Converter for Soft-Switching Range Extension," *Energy Conversion Congress and Exposition (ECCE)*, 2016.
- [10] J. Huang, Z. Li, L. Shi, Y. Wang, and J. Zhu, "Optimized Modulation and Dynamic Control of a Three-Phase Dual Active Bridge Converter with Variable Duty Cycles," *IEEE Trans. Power Electron.*, vol. 34, no. 3, pp. 2856–2873, 2019.
- [11] N. H. Baars, C. G. Wijnands, and J. Everts, "A Three-Level Three-Phase Dual Active Bridge DC-DC Converter with a Star-Delta Connected Transformer," *2016 IEEE Vehicle Power and Propulsion Conference, VPPC 2016 - Proceedings*, pp. 1–6, 2016.
- [12] N. H. Baars, J. Everts, C. G. E. Wijnands, and E. A. Lomonova, "Evaluation of a High-Power Three-Phase Dual Active Bridge DC-DC Converter with Three-Level Phase-Legs," in *2016 18th European Conference on Power Electronics and Applications (EPE'16 ECCE Europe)*, 2016.
- [13] N. H. Baars, C. G. Wijnands, and J. Everts, "ZVS modulation strategy for a three-phase dual active bridge DC-DC converter with three-level phase-legs," *2016 18th European Conference on Power Electronics and Applications, EPE 2016 ECCE Europe*, pp. 1–10, 2016.
- [14] B. Jin and X. Yuan, "Topology, Efficiency Analysis, and Control of a Four-Level  $\pi$  -Type Converter," *IEEE Trans. Emerg. Sel. Topics Power Electron.*, vol. 7, no. 2, pp. 1044–1059, 2019.
- [15] H. M. De Oliveira Filho, D. D. S. Oliveira, and P. P. Praça, "Steady-state analysis of a ZVS bidirectional isolated three-phase DC-DC converter using dual phase-shift control with variable duty cycle," *IEEE Trans. Power Electron.*, vol. 31, no. 3, pp. 1863–1872, 2016.
- [16] G. Waltrich, M. A. M. Hendrix, and J. L. Duarte, "Three-Phase Bidirectional DC/DC Converter with Six Inverter Legs in Parallel for EV

- Applications," *IEEE Trans. Ind. Electron.*, vol. 63, no. 3, pp. 1372–1384, 2016.
- [17] R. N. Oliveira, L. Mazza, H. Oliveira Filho, and D. de Souza Oliveira, "A Three-Port Isolated Three-Phase Current-Fed DC-DC Converter Feasible to PV and Storage Energy System Connection on a DC Distribution Grid," *IEEE Trans. Ind. Appl.*, vol. 55, no. 5, pp. 1–1, 2019.
- [18] J. Voss and R. W. A. A. De Doncker, "Modified Auxiliary-Resonant Commutated Pole Applied in a Three-Phase Dual-Active Bridge DC/DC Converter," *IEEE Trans. Power Electron.*, vol. PP, no. c, pp. 1–1, 2019.
- [19] A. N. Rahman, C. Y. Lee, H. J. Chiu, and Y. C. Hsieh, "Bidirectional Three-Phase LLC Resonant Converter," *ITEC Asia-Pacific 2018 - 2018 IEEE Transportation Electrification Conference and Expo, Asia-Pacific: E-Mobility: A Journey from Now and Beyond*, pp. 1–5, 2018.
- [20] M. Noah, S. Kimura, W. Imaoka, W. Martinez, S. Endo, M. Yamamoto, and K. Umetani, "Magnetic Design and Experimental Evaluation of a Commercially Available Single Integrated Transformer in Three-Phase LLC Resonant Converter," *IEEE Trans. Ind. Appl.*, vol. 54, no. 6, pp. 6190–6204, 2018.
- [21] A. Z. Khan, K. H. Loo, and Y. M. Lai, "Design, Analysis and Performance Characterization of Dual-Active-Bridge DC-DC Converter Utilizing Three-Phase Resonant Immittance Network," *IEEE Trans. Power Electron.*, vol. 34, no. 2, pp. 1159–1180, 2018.
- [22] A. K. Bhat and R. L. Zheng, "Analysis and design of a three-phase LCC-type resonant converter," *IEEE Trans. Aerosp. Electron. Syst.*, vol. 34, no. 2, pp. 508–519, 1998.
- [23] A. Z. Khan and K. H. Loo, "A Three-Phase Dual-Active-Bridge DC-DC Immittance Converter," *2018 IEEE Energy Conversion Congress and Exposition (ECCE)*, pp. 6336–6343, 2018.
- [24] H. Irie and S. Ohashi, "A three-phase constant-current source using an immittance converter," *Electrical Engineering in Japan*, vol. 151, no. 4, pp. 47–54, 2005. [Online]. Available: <http://doi.wiley.com/10.1002/eej.20078>
- [25] H. Irie and T. Nishisako, "Three-phase immittance converter," *Electrical Engineering in Japan*, vol. 145, no. 1, pp. 52–58, 2003. [Online]. Available: <http://doi.wiley.com/10.1002/eej.10169>
- [26] A. Z. Khan and K. H. Loo, "A Three-Phase Dual-Active-Bridge DC-DC Converter with Reconfigurable Resonant Network for Efficient Wide Voltage Range Operation," *IEEE Trans. Power Electron.*, vol. 35, no. 2, pp. 1322–1339, 2020.
- [27] R. Mirzahosseini and F. Tahami, "A Phase-Shift Three-Phase Bidirectional Series Resonant DC / DC Converter," in *IECON 2011 - 37th Annual Conference of the IEEE Industrial Electronics Society*, 2011, pp. 1137–1143.
- [28] A. Z. Khan and K. H. Loo, "A reconfigurable three-phase dual-active-bridge DC-DC converter designed for wide-range high-efficiency operation," *PEDG 2019 - 2019 IEEE 10th International Symposium on Power Electronics for Distributed Generation Systems*, pp. 147–154, 2019.
- [29] M. Yaqoob, K. H. Loo, and Y. M. Lai, "Fully Soft-Switched Dual-Active-Bridge Converter with Switched-Impedance-Based Power Control," *IEEE Trans. Power Electron.*, vol. 8993, no. c, pp. 1–16, 2018.
- [30] W. J. Gu and K. Harada, "A new method to regulate resonant converters," *IEEE Trans. Power Electron.*, vol. 3, no. 4, pp. 430–439, 1988.
- [31] F. Krismer and J. Kolar, "Accurate Power Loss Model Derivation of a High-Current Dual Active Bridge Converter for an Automotive Application," *IEEE Trans. Ind. Appl.*, vol. 57, no. 3, pp. 881–891, 2010.
- [32] M. Yaqoob, K. H. Loo, Y. P. Chan, and J. Jatskevich, "Optimal Modulation for a Fifth-Order Dual-Active-Bridge Resonant Immittance DC-DC Converter," *IEEE Trans. Power Electron.*, vol. 35, no. 1, pp. 1–1, 2019.



**Akif Zia Khan** (S'15) received the B.Eng. degree in electrical engineering from the National University of Sciences and Technology (NUST), Islamabad, Pakistan, the M.Sc. degree in electric power systems from the North China Electric Power University, Beijing, and the Ph.D. degree in power electronics from The Hong Kong Polytechnic University, Hong Kong in 2011, 2014, and 2020, respectively. From 2014 to 2016, he has served as a Lecturer at U.S.-Pakistan Center for Advanced Studies in Energy, NUST, Islamabad, Pakistan. During the spring of 2016, he was a visiting research scholar with the Power Systems Engineering Research Center, Arizona State University, Tempe, AZ, USA. During 2019, he was a visiting PhD scholar with the Center of Reliable Power Electronics, Department of Energy Technology, Aalborg University, Denmark. He is currently serving as a Lecturer at U.S.-Pakistan Center for Advanced Studies in Energy, NUST, Islamabad, Pakistan. His current research interests include design, modeling and control of high-power and high-frequency bidirectional resonant power converters for various power processing applications. He contributes regularly as a reviewer for various international journals and conferences.

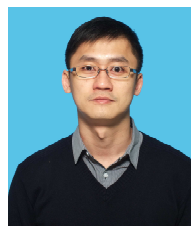


**Yiu Pang Chan** (S'17) received the B.Sc. (Hons.) degree in engineering physics in 2015 from The Hong Kong Polytechnic University, where he is currently working toward the Ph.D. degree in the area of power electronics. His research interests include high frequency bidirectional isolated dc-dc and ac-dc converters.



reviewer of IEEE TRANSACTIONS ON POWER ELECTRONICS and IEEE TRANSACTIONS ON INDUSTRIAL ELECTRONICS.

**Muhammad Yaqoob** (S'16–M'19) received the B.Eng. degree in electronics engineering from the National University of Sciences and Technology, Islamabad, Pakistan, in 2012 and the Ph.D. degree in power electronics from The Hong Kong Polytechnic University, Hong Kong, in 2018. He is currently working as a Senior Power Electronics Engineer at Huawei Research Center, Stockholm, Sweden. His current research interests include high frequency bidirectional isolated dc-dc and ac-dc converters for electric vehicle applications. Mr. Yaqoob serves as a



**Ka-Hong Loo** (S'97–M'99) received the B.Eng. (Hons.) in electronic engineering and the Ph.D. degree from the University of Sheffield, U.K., in 1999 and 2002, respectively. Upon completion of his doctoral degree, he won the Japan Society for the Promotion of Science (JSPS) Postdoctoral Fellowship and worked as postdoctoral researcher at Ehime University, Japan, from 2002 to 2004. He joined The Hong Kong Polytechnic University in 2006 where he is now an Associate Professor in the Department of Electronic and Information Engineering.

His research interests include high-frequency power conversion, in particular power converters for renewable energy systems. He has been an Associate Editor for the IEEE Transactions on Energy Conversion since 2013 and IEEE Power Engineering Letters since 2015, and contributes regularly as reviewer for various international journals and conferences. He is currently the Chair of the Power Electronics and Control Sub-Committee of the IEEE Technical Committee on Transportation Electrification.



**Pooya Davari** (S'11–M'13–M'19) received the B.Sc. and M.Sc. degrees in electronic engineering in 2004 and 2008, respectively, and the Ph.D. degree in power electronics from QUT, Australia, in 2013. From 2005 to 2010, he was involved in several electronics and power electronics projects as a Development Engineer. From 2013 to 2014, he was with QUT, as a Lecturer. He joined Aalborg University, in 2014, as a Postdoc, where he is currently an Associate Professor.

He has been focusing on EMI, power quality and harmonic mitigation analysis and control in power electronic systems. He has published more than 140 technical papers. Dr. Davari served as a Guest Associate Editor of IET journal of Power Electronics, IEEE Access Journal, Journal of Electronics and Journal of Applied Sciences. He is an Associate Editor of Journal of Power Electronics, Associate Editor of IET Electronics, Editorial board member of EPE journal and Journal of Applied Sciences. He is member of the International Scientific Committee (ISC) of EPE (ECCE Europe) and a member of Joint Working Group six and Working Group eight at the IEC standardization TC77A. Dr. Davari is the recipient of a research grant from the Danish Council of Independent Research (DFF-FTP) in 2016, and 2020 IEEE EMC Society Young Professional Award for his contribution to EMI and Harmonic Mitigation and Modeling in Power Electronic Applications.



**Frede Blaabjerg** (S'86–M'88–SM'97–F'03) was with ABB-Scandia, Randers, Denmark, from 1987 to 1988. From 1988 to 1992, he got the PhD degree in Electrical Engineering at Aalborg University in 1995. He became an Assistant Professor in 1992, an Associate Professor in 1996, and a Full Professor of power electronics and drives in 1998. From 2017 he became a Villum Investigator. He is honoris causa at University Politehnica Timisoara (UPT), Romania and Tallinn Technical University (TTU) in Estonia.

His current research interests include power electronics and its applications such as in wind turbines, PV systems, reliability, harmonics and adjustable speed drives. He has published more than 600 journal papers in the fields of power electronics and its applications. He is the co-author of four monographs and editor of ten books in power electronics and its applications.

He has received 32 IEEE Prize Paper Awards, the IEEE PELS Distinguished Service Award in 2009, the EPE-PEMC Council Award in 2010, the IEEE William E. Newell Power Electronics Award 2014, the Villum Kann Rasmussen Research Award 2014, the Global Energy Prize in 2019 and the 2020 IEEE Edison Medal. He was the Editor-in-Chief of the IEEE TRANSACTIONS ON POWER ELECTRONICS from 2006 to 2012. He has been Distinguished Lecturer for the IEEE Power Electronics Society from 2005 to 2007 and for the IEEE Industry Applications Society from 2010 to 2011 as well as 2017 to 2018. In 2019-2020 he serves as the President of IEEE Power Electronics Society. He is Vice-President of the Danish Academy of Technical Sciences too. He is nominated in 2014-2019 by Thomson Reuters to be between the most 250 cited researchers in Engineering in the world.